The WELDING JOURNAL

NOVEMBER 1953

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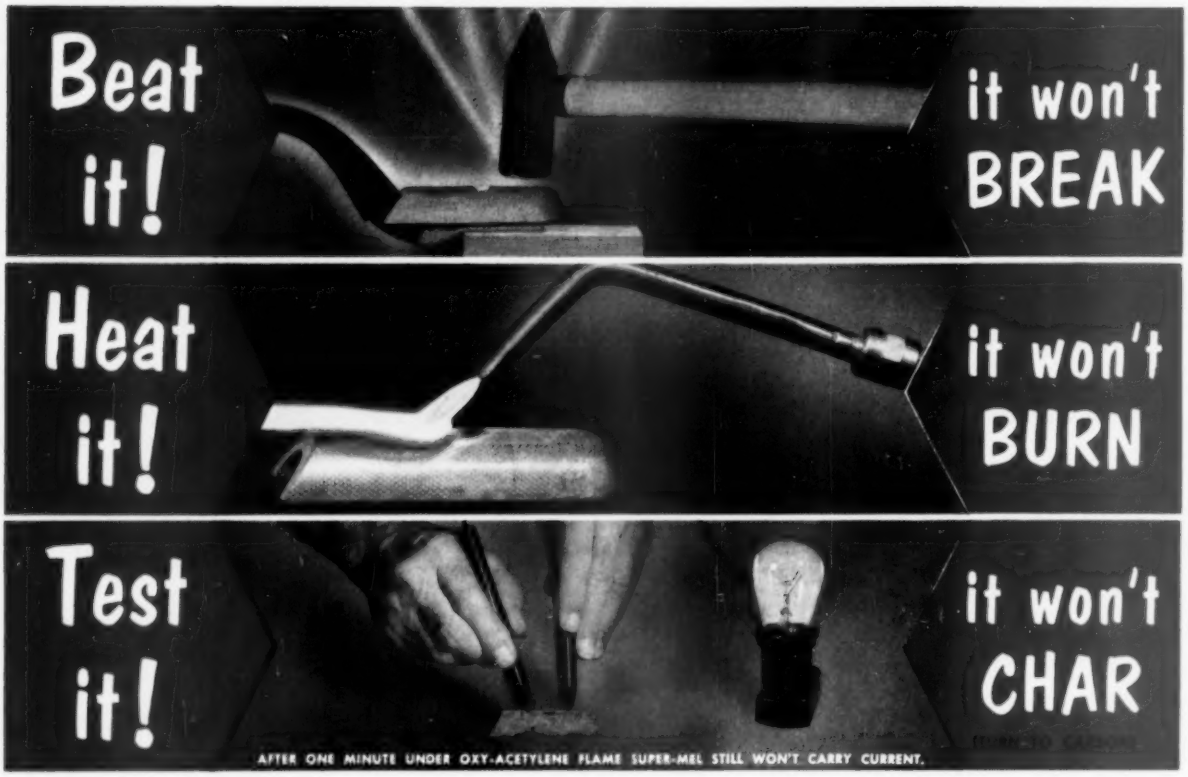

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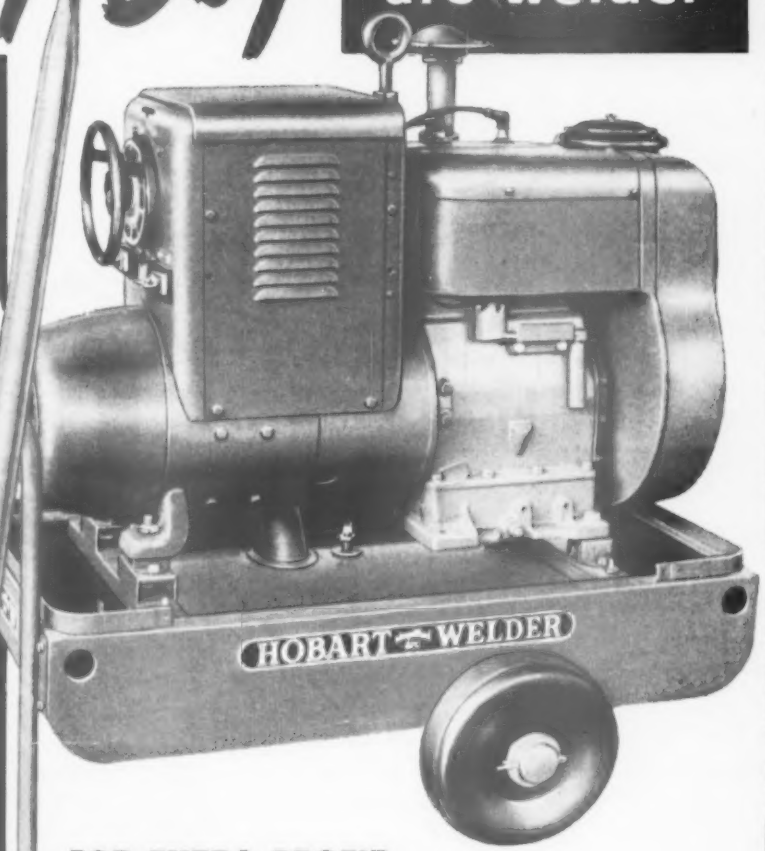

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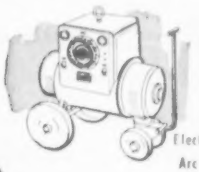
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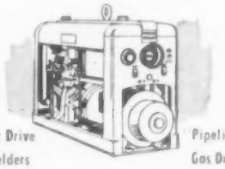
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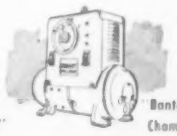
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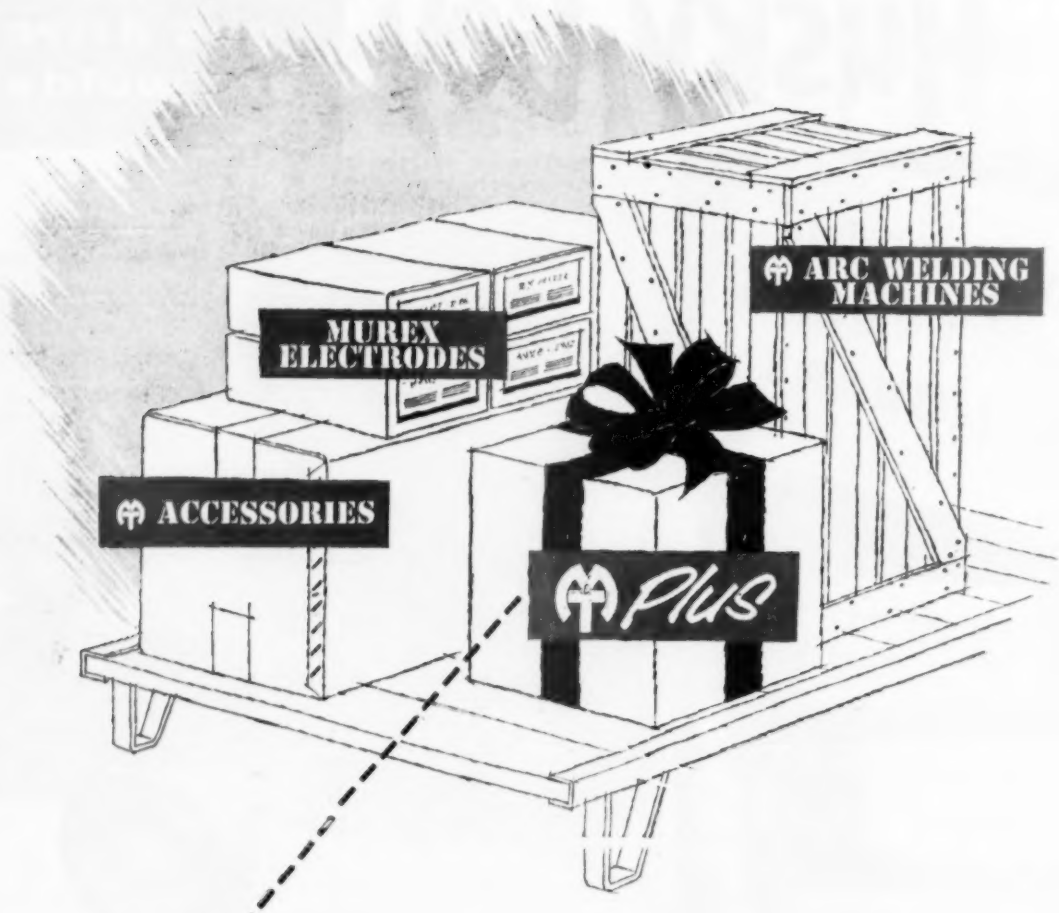

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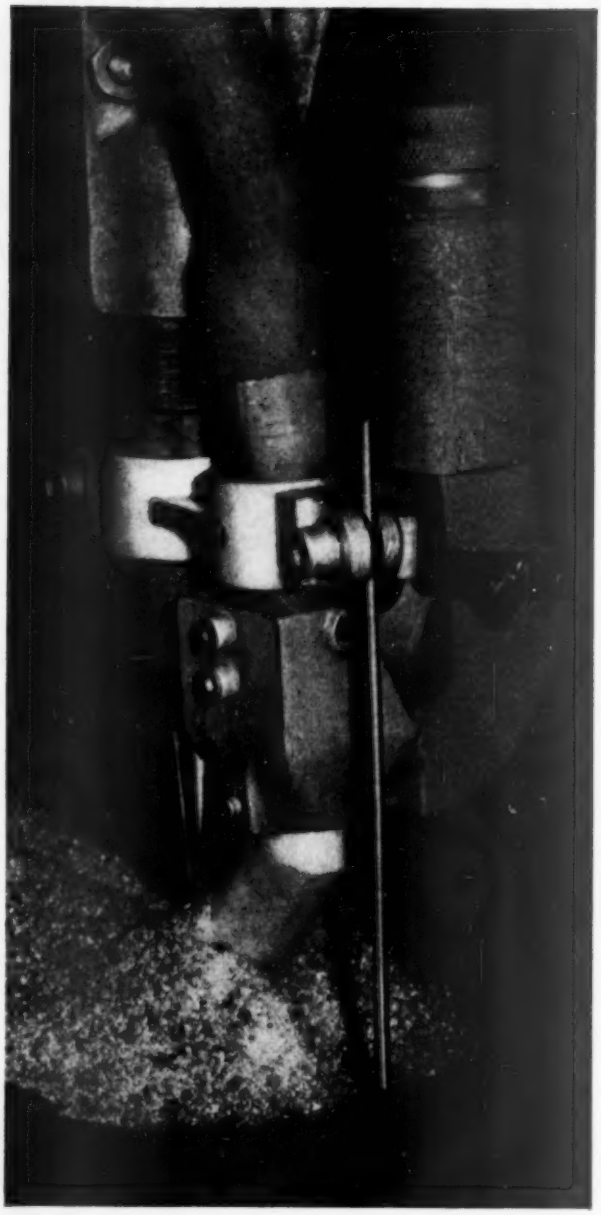

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The WELDING JOURNAL

volume 32 November 1953 number 11

Spot Welding of Titanium-Carbon Alloys

An investigation on spot welding of titanium-carbon alloys containing 0.1, 0.4 and 0.6% carbon in sheet thicknesses of 0.038–0.047 in. including surface preparation, physical properties and welding conditions

by E. F. Holt, F. H. Vandenburgh and N. L. McClymonds

I. INTRODUCTION

AN INVESTIGATION has been conducted in the spot welding of three grades of titanium-carbon sheet, specifically 0.1, 0.4 and 0.6% carbon in sheet thicknesses of 0.038–0.047 in.

Scope of the investigation covered surface preparation, contact resistance, weld strength and ductility, weld geometry and quality, effect of welding variables on weld properties and establishment of recommended welding conditions. Limited comparison tests were made on stainless steel.

It is understood that the trend is to lower carbon content in commercially pure titanium to improve the weldability and, particularly, improve the weld ductility. To be determined by future investigation is the effect of carbon and other interstitial compounds in combination with minor additions of alloy elements on the spot weldability of the material.

II. TITANIUM-CARBON SHEET PROPERTIES AND PREPARATION

The average properties of the titanium sheet used in the investigation are presented in Table 1. The titanium was supplied in the form of hot-rolled, annealed and pickled sheet.

TABLE 1

AVERAGE PROPERTIES OF TITANIUM-CARBON ALLOYS

PROPERTY \ ALLOY	0.1% CARBON	0.4% CARBON	0.6% CARBON
THICKNESS (IN.)	0.040	0.038	0.047
U.T.S. (P.S.I.)	77,000	84,000	121,000
Y.S. (P.S.I.)	60,000	67,000	111,000
PROP. LIMIT (P.S.I.)	43,000	48,000	79,000
ELONG. (% IN 2")	20.1	25	11.5
MODULUS (P.S.I. X 10^6)	17.2	16.2	15.0
HARDNESS (ROCK. A)	55	55	65

Pertinent manufacturing data are disclosed in Table 2. Prior to any surface treatment or welding, the "as received" material was cleaned using carbon tetrachloride.

III. WELD-SPECIMEN DESIGN AND TESTING

Tension-shear and cross tension specimens were patterned closely to AWS recommended design and are shown in Fig. 1. Specimens were welded in suitable aligning fixtures and breaking loads were determined using a 3000-lb Olsen hydraulic tensile test machine.

Metallographic specimens from which was determined weld diameter, penetration, quality, etc., were

E. F. Holt is the Chief Engineer, Welding Division, of P. R. Mallory & Co., Inc.; **F. H. Vandenburgh** is Vice-President and General Manager of the Mallory-Sharon Titanium Corp.; and **N. L. McClymonds** is a Metallurgical Engineer of P. R. Mallory & Co., Inc.

Presented at the AWS National Spring Meeting, Houston, Tex., June 16–19, 1953.

TABLE 2

MFG. DATA ON TITANIUM-CARBON ALLOYS

ALLOY / CONDITIONS	0.1% CARBON	0.4% CARBON	0.6% CARBON
THICKNESS (IN)	0.040	0.038	0.047
TYPE MELT	ARC	INDUCTION	INDUCTION
CHEMICAL COMP. (%)			
CARBON	0.10	0.40	0.60
NITROGEN	0.007	0.013	0.04
IRON	0.13	—	0.068
BILLET SOURCE	BATTELLE	MALLORY-SHARON	MALLORY-SHARON
SHEET SOURCE*	MALLORY-SHARON	MALLORY-SHARON	MALLORY-SHARON

* SHEET RECEIVED AS-HOT ROLLED, ANNEALED AND PICKLED

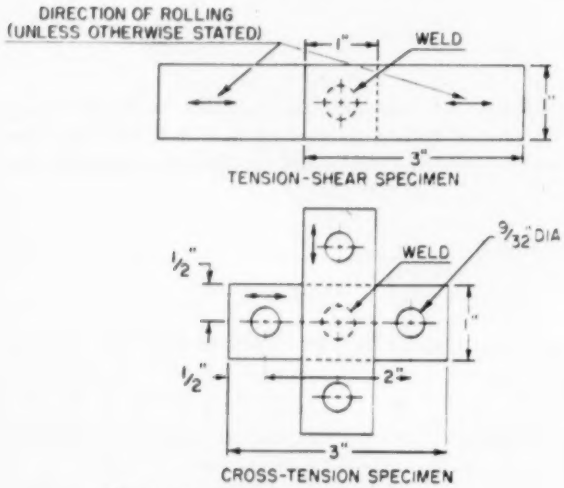


Fig. 1 Spot weld strength specimens

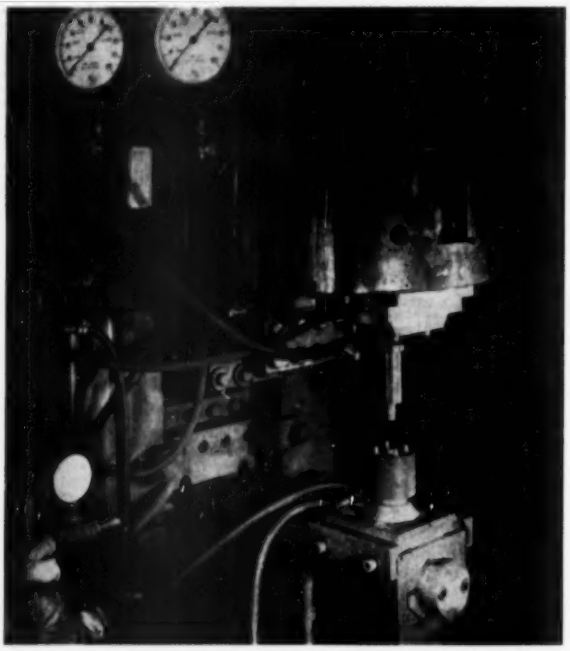


Fig. 2 Welder setup (cross-tension fixture in position)

TABLE 3

RESISTANCE WELDING EQUIPMENT

WELDER	PRESS TYPE - MOTOR OPERATED AIR CUSHION HEAD - SPOT & PROJ.
RATING	150 KVA - SINGLE PHASE A.C.
FRAME	R.W.M.A. SIZE 3
CONTROL	SYNCHRONOUS - PHASE SHIFT
ELECTRODE MOUNTING	ON PLATENS - PLATEN HOLDERS
PRESSURE	CUSHION TYPE AIR HEAD LOW INERTIA RAM.

polished and etched using a 10% Hydrofluoric acid solution.

IV. SPOT-WELDING EQUIPMENT AND ELECTRODES

All welds were made using a standard single phase A-C combination spot and projection welding machine, described in Table 3. The electrodes were mounted in platen type holders to minimize deflection and skidding.

Figure 2 is a photograph of the welder, and Fig. 3 shows the design of the electrodes.

Electrodes were made from a single mill length of Mallory 3 rod of known properties (Table 4).

Welding machine operation and conditions were monitored using Brush recorders and welding was done in a manner to minimize machine variables.

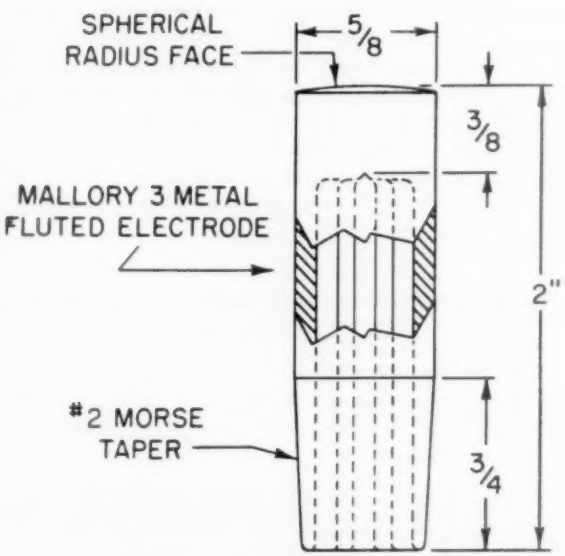


Fig. 3 Electrode design

TABLE 4

ELECTRODE DATA

MATERIAL	MALLORY 3
U.T.S. (P.S.I.)	83,000
ELONG. (% IN 2")	12.5
HARDNESS (ROCK. B)	85
ELECT. COND. (% I ACS)	84
TYPE	FLUTED HOLE
SIZE	*2 MORSE - 5/8" DIA.
DESIGN	FIG. 3
MALLORY CODE NO.	SIMILAR TO F-3825

V. SURFACE PREPARATION AND CONTACT RESISTANCE

(a) *Procedure*

To determine the effects of surface preparation on the probable weldability of the titanium, contact resistance studies were made using a variety of surface preparation methods.

Sheet to sheet contact resistance was determined using a contact resistance measuring device (Fig. 4) similar to that generally used to determine contact resistance on sheet aluminum alloys. The conditions for measuring the resistance were:

Electrode material	Mallory 3
Electrode face contour	4 in. radius
Force	500 lb
Current	20 amp dc
Specimen size	1 x 3 in.
Measuring instrument	Microhm meter

The surface preparations are listed in Table 5.

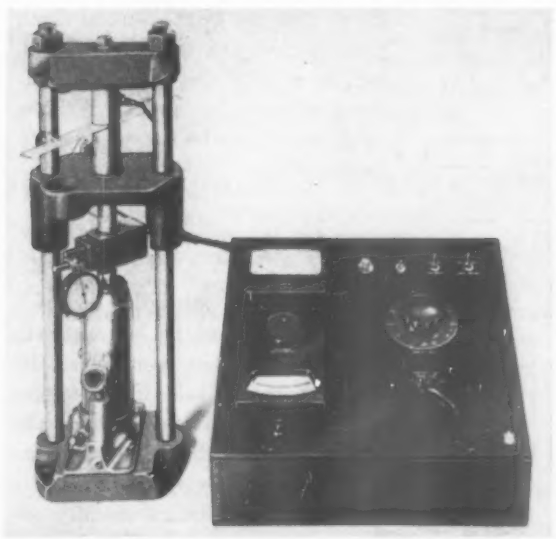


Fig. 4 Contact-resistance measuring device

TABLE 5

DESCRIPTION OF SURFACE TREATMENTS

GROUP NO.	TREATMENT * DESIGNATION	SURFACE TREATMENT *
1	AS RECEIVED	NONE
2	MECHANICAL	SAND USING 240 GRIT ALOXITE ABRASIVE CLOTH
3	ACID	5% HF + 5% HNO_3 + WATER 20° C.- 5 TO 15 MINUTES RINSE IN WATER
4	HYDRIDE	FUSED NaOH + Na + H_2 RINSE IN HOT WATER
5	HYDRIDE + ACID	FUSED NaOH + Na + H_2 HOT RINSE FOLLOWED BY 14% HF + 28% H_2SO_4 + WATER
6	AS ANNEALED	ANNEALED ½ HOUR AT 700° C IN AIR

* MATERIAL INITIALLY IN "AS RECEIVED" CONDITION AND CLEANED IN CCl_4

(b) *Results*

Table 6 lists results of contact resistance measurements on 0.4% C sheet. Also included in Table 6 are results obtained on Type 302 stainless steel 0.040 in. thick, C.R. bright finish 90RB sheet.

Studies on the 0.1% C and 0.6% C sheet showed no significant difference from results on the 0.4% C sheet.

Contact resistance of material cleaned by any of the methods was unaffected by exposure to air up to 3 hr. Lowest and most consistent contact resistance was obtained by "acid" and "Hydride plus acid" cleaning. Specimens as "Hydride" cleaned were significantly different, having a relatively high and erratic contact resistance, possibly caused by incomplete removal of the cleaning salt. Further treatment in acid was required. The extremely high-contact resistance of the "as annealed" specimens indicated that cleaning would be necessary prior to spot welding.

The contact resistance of the stainless steel appears to be considerably higher than on the "as received" titanium.

TABLE 6

SHEET TO SHEET CONTACT RESISTANCE *
(500 LBS. FORCE - 4" R. ELECTRODES - 20 AMPS D.C.)

GROUP NO.	TREATMENT DESIGNATION	AVERAGE CONTACT RESISTANCE (MICROHMS)	STANDARD DEVIATION (MICROHMS)
1	AS RECEIVED	268	263
2	MECHANICAL	116	115
3	ACID	55	13
4	HYDRIDE	3780	1631
5	HYDRIDE + ACID	42	13
6	AS ANNEALED	15,500	3600
7	STAINLESS STEEL TYPE 302 0.040"- BRIGHT FINISH	998	850

* ON 0.4% CARBON TITANIUM

VI. EFFECT OF CLEANING METHOD ON SOME WELD PROPERTIES

(a) *Procedure*

To determine the effect of surface preparation on weld strength, a series of specimens having the various surface preparations were spot welded and tested. A preliminary study established a set of welding conditions which, while possibly not optimum, were found to produce spot welds of satisfactory strength, quality and consistency. These conditions were:

Weld time	10 cycles
Weld force	1000 lb
Weld current	9000 amp, approx.
Electrode contour	3 in. spherical radius

Although the "as received" material had a somewhat higher contact resistance than the "acid cleaned," it might be sufficiently low to provide for practical purposes (production welding) welds of acceptable strength and consistency. Hence, a limited statistical survey was made of shear strength values on both "as received" and "acid cleaned" specimens.

(b) *Results*

Based on weld strength, there was no apparent interaction between cleaning method and carbon content. Table 7 confirms this conclusion generally, although some discrepancies exist, particularly in the ductility ratio. Of note is the apparent reduction in weld ductility of the titanium cleaned by "Hydride" and "Hydride plus acid," particularly in the 0.1% C sheet, while the higher carbon sheets did not seem so affected. Possibly subsurface contamination of the 0.1% C sheet occurred during the "Hydride" cleaning, although no thorough examination was made. Welds made in the "Hydride" cleaned material were generally erratic, indicating the necessity of more thorough rinsing for complete removal of the salt.

TABLE 7

EFFECT OF SURFACE TREATMENT ON WELD STRENGTH ***

GROUP	SURFACE TREATMENT DESIGNATION	BREAKING LOAD - POUNDS **								
		0.1% C			0.4% C			0.6% C		
		SHEAR	TENSION	T/S	SHEAR	TENSION	T/S	SHEAR	TENSION	T/S
1	AS RECEIVED	2297	1186	.52	2000	603	.30	2420	404	.17
2	MECHANICAL	2280	1133	.50	2260	1006	.45	1990	398	.20
3	ACID	2402	987	.41	2123	568	.27	2340	498	.21
4	HYDRIDE	2180	710	.33	1960	627	.32	2180	399	.18
5	HYDRIDE + ACID	2463	821	.33	2007	715	.36	2520	400	.16
6	AS ANNEALED *	—	—	—	—	—	—	—	—	—

* UNSATISFACTORY WELD CONSISTENCY.
** AVERAGE VALUES
*** WELDING CONDITIONS - 10 CYCLE, 1000 LB FORCE, 9000 AMPS, 3" RADIUS ELECTRODE

TABLE 8

SHEAR STRENGTH AND CONSISTENCY AS AFFECTED BY SURFACE CONDITION (0.4% C TITANIUM)

SURFACE PREPARATION	AVERAGE OF STRENGTHS SHEAR (LBS)	STANDARD DEVIATION (LBS)	RANGE OF SHEAR STRENGTHS (LBS)
AS RECEIVED	2250	86.3	2100 TO 2390
ACID CLEANED	2348	49.0	2270 TO 2430

Results of the statistical survey of the effect of surface preparation on the shear strength (data in Table 8) using "as received" and "acid cleaned" material (0.4% C) shows that the "acid cleaned" material was productive of welds having slightly higher strength (about 4%) and with less variation. This was expected in view of the lower and more uniform contact resistance of the "acid cleaned" stock.

The data in Table 8 indicate that the increase in weld strength and consistency using acid cleaning was deemed insufficient to warrant the added cost, and for the balance of the tests (welding conditions study) the titanium was welded in the "as received" condition.

VII. EFFECT OF DIRECTION OF ROLLING ON WELD STRENGTH

(a) *Procedure*

The three carbon grades of titanium were spot welded into specimens in three different manners with respect to direction of rolling: first, both pieces with the grain; second, both pieces across the grain and, third, one piece with the grain and one piece across the grain. Welding conditions were the same as used in Section VI.

(b) *Results*

Tension-shear strengths disclosed the breaking load to be relatively unaffected by direction in which the titanium was rolled. Table 9 lists breaking loads averaged from the three titanium-carbon alloys, and further averaged from material having three acceptable surface conditions—"as received," "mechanically cleaned"

TABLE 9

EFFECT OF DIRECTION OF ROLLING ON WELD STRENGTH

GRAIN ORIENTATION OF SPECIMENS	BREAKING LOAD IN SHEAR* -POUNDS
BOTH PIECES GRAIN LONGITUDINAL	2280
BOTH PIECES GRAIN TRANSVERSE	2268
ONE PIECE WITH GRAIN ONE PIECE ACROSS GRAIN	2209

* AVERAGE VALUES

and "acid cleaned." It seems safe to assume that under static loading (shear or normal tension) spot weld strength is little affected by direction of rolling.

VIII. HARDNESS OF THE WELD AREA

Microhardness surveys made on sectioned welds in the titanium-carbon alloys using "as received" stock and welding conditions given in Section VI show that (Table 10) the hardness is little affected by welding either in the

TABLE 10

MICRO-HARDNESS SURVEYS ON SECTIONED SPOT WELDS

TITANIUM SHEET	AVERAGE VICKERS (VDP) HARDNESS *		
	BASE METAL	HEAT AFFECTED ZONE	WELD NUGGET
0.1% C	203	216	205
0.4% C	205	229	227
0.6% C	272	306	279

*EACH FIGURE AVERAGE AT 12 READINGS

heat affected zone or the weld nugget. Slight increase in hardness occurs in the 0.4% C material, while the increase is more pronounced in the 0.6% C titanium. In all cases the extent of hardening is quite low.

IX. ESTABLISHMENT OF SUITABLE WELDING CONDITIONS

Conditions selected on a preliminary basis, while producing welds of good strength and quality, were not necessarily optimum conditions and, hence, a more complete survey was made to establish optimum conditions. Since the three grades of titanium appeared to react similarly, one material—the 0.4% C—was selected for welding condition surveys.

(A) Effect of Weld Force and Electrode Contour

(*a*) *Procedure.* A survey was made to select the best electrode contour and electrodes having 3, 6 and 10-in. spherical radius face contours were used. Three weld forces were used. The conditions were:

Material	0.4% C sheet
Condition	"As received" (cleaned in CCl_4)
Weld time	10 cycles
Welding current	Approx. 9000 amp
Weld force	600, 900 and 1200 lb

(*b*) *Results.* There appeared to be no significant difference in weld strength, quality and consistency due to electrode contour as indicated in Table 11. Welds made at the higher forces seemed more consistent and of higher strength. Table 12 lists data obtained from metallographic examination (of welds made at 1200 lb force with each of the electrodes) and, based on weld properties, either of the electrode contours can be used with satisfactory results. While the data in Tables 11 and 12 are averages, original test results showed no statistical preference for any one electrode contour.

A characteristic of spot welds in titanium is high weld penetration and this is not appreciably affected by variations in electrode contour. Should life tests be run on electrodes of different contours, it may be possible to select a particular contour as most resistant to deformation. It was concluded that:

1. There was no significant difference in strengths due to electrode contour.

TABLE 11

TENSION-SHEAR STRENGTH AS AFFECTED BY ELECTRODE CONTOUR & WELD FORCE *

ELECTRODE CONTOUR	BREAKING LOAD IN SHEAR (LBS.) **		
	FORCE - 600 LBS	FORCE - 900 LBS.	FORCE - 1200 LBS.
3" RADIUS	1995	1975	2130
6" RADIUS	2025	1985	2105
10" RADIUS	1950	1955	2215

* 0.4% C TITANIUM SHEET
** AVERAGE VALUES

TABLE 12

WELD CHARACTERISTICS AS AFFECTED BY ELECTRODE CONTOUR (1200 LB. WELD FORCE)

ELECTRODE CONTOUR	WELD NUGGET DIAMETER (IN)	WELD PENETRATION (%)	HEAT AFFECTED ZONE PENETRATION (%)	INDENTATION (%)
3" RADIUS	0.250	86	94	11
6" RADIUS	0.255	87	97	12
10" RADIUS	0.243	86	92	12

NOTE - ALL VALUES ARE AVERAGE
- MATERIAL 0.4% C, 10 CYCLES, 1200 LBS., 9000 AMPS APPROX.

2. No interaction could be detected between weld force and electrode contour.

Metallographic examination disclosed:

1. All weld nuggets possessed columnar structures.
2. No evidence of porosity. Sheet separation was negligible and no surface or interfacial expulsion was encountered.

(B) *Effect of Weld Time and Weld Force*

(*a*) *Procedure.* A survey was made to bracket optimum weld time and optimum weld force employing conditions arrived at by foregoing experiments and varying weld time and weld force. The following conditions were used:

Material	0.4% C titanium
Surface preparation	"As received" (cleaned in CCl_4)
Electrode contour	3 in. spherical radius
Weld time	6, 12, 18 and 24 cycles
Weld force	800, 1400 and 2000 lb
Weld current	9000 to 10,000 amp approx.

A series of seven specimens were welded at each combination of time and force, three were tested in tension-shear, three in cross tension and one for metallographic examination. In addition, specimens of stainless steel Type 302, 0.40-in. thick, C.R. bright finish, 90 Rockwell B, were welded and tested.

(*b*) *Results.* Results of shear tests are listed in Table 13. All specimens pulled slugs upon fracture and were of good quality and consistency. Analysis of variance in shear data (Table 13) and original measurements indicated that:

1. There was no statistical difference in strengths due to welding force.
2. The existence of an interaction between weld force and weld time was doubtful.
3. Weld strength increases moderately with weld time.

Initially it was intended that under any set of welding conditions the weld current would be adjusted to a value to produce shear strength of about 2000 lb with a nugget diameter of about 0.250 in., while avoiding undesirable characteristics such as expulsion, electrode sticking, excessive indentation, etc. An unusual feature of this series of tests was that it was unnecessary to alter the heat setting of the machine, which was set on Heat Tap 2—50% heat.

Table 14 lists results of cross-tension tests and:

1. There was no significant difference in strengths due to weld time.
2. There was a doubtful difference in strength associated with weld force.
3. No interaction was detected between weld force and weld time.

Ductility ratios (tension/shear) were calculated (Table 15) showing ductility decreases with increased weld force, being near maximum with a weld time of 12 cycles. However, the maximum difference is not great, ranging from 0.21 to 0.32. Microstructure of sectioned welds provided the data in Table 16, which shows that weld force does not appreciably affect the weld size, indentation or sheet separation. Weld

TABLE 13

SHEAR STRENGTH AS AFFECTED BY WELD FORCE & WELD TIME

WELD TIME (CYCLES)	BREAKING LOAD-LBS.*		
	WELD FORCE 800 LBS.	WELD FORCE 1400 LBS.	WELD FORCE 2000 LBS.
6	1957	1907	1970
12	2153	2180	2023
18	2290	2437	2127
24	2223	2160	2367

*AVERAGE OF THREE TESTS
MATERIAL-0.4% C TITANIUM

TABLE 14

CROSS TENSION STRENGTH AS AFFECTED BY WELD FORCE AND WELD TIME

WELD TIME (CYCLES)	BREAKING LOAD-LBS.*		
	WELD FORCE 800 LBS.	WELD FORCE 1400 LBS.	WELD FORCE 2000 LBS.
6	583	511	421
12	684	609	524
18	519	538	474
24	593	581	583

*AVERAGE OF THREE TESTS
MATERIAL-0.4%C TITANIUM

TABLE 15

CROSS TENSION / TENSION-SHEAR RATIOS AS AFFECTED BY WELD FORCE AND WELD TIME *

WELD TIME (CYCLES)	TENSION / TENSION-SHEAR		
	WELD FORCE 800 LBS.	WELD FORCE 1400 LBS.	WELD FORCE 2000 LBS.
6	0.30	0.27	0.21
12	0.32	0.28	0.26
18	0.23	0.22	0.22
24	0.27	0.27	0.25

* CALCULATED FROM TABLES 13 & 14

TABLE 16

EFFECT OF WELD FORCE AND WELD TIME ON CHARACTERISTICS*

WELD FORCE (LBS)	WELD TIME (CYCLES)	NUGGET DIA. (IN.)	% PENETRATION OF NUGGET	% PENETRATION OF HEAT AFFECTED ZONE	% INDENTATION	SHEET SEPARATION (IN.)
800	6	.220	79	90	9	.002
	12	.270	80	93	12	.002
	18	.265	82	94	10.5	.002
	24	.280	81	91	10.5	.002
1400	6	.240	80	91	8	.002
	12	.250	75	91	12	.002
	18	.265	78	88	10.5	.002
	24	.280	77	91	13	.002
2000	6	.235	79	91	10.5	.002
	12	.250	79	90	10.5	.002
	18	.265	74	96	10.5	.002
	24	.280	80	91	12	.002

* WELD STRENGTHS GIVEN IN TABLES 13 & 14
MATERIAL - 0.4% C TITANIUM - 0.038" THICK

nugget diameter is increased with increasing weld time. Weld penetration and sheet separation are affected little by either weld time or weld force.

X-ray and metallographic examination showed no expulsion (weld splash), porosity, cracks or inclusions.

Companion data on stainless steel welds are listed in Table 17. Similar trends occurred in weld strength with force and time in the stainless steel as in the 0.4% C titanium. About 12% higher shear strengths were obtained in the stainless and the tension strength was substantially higher (about 240%), resulting in a T/S ratio of about 0.75. Averaging all data compiled in the weld time-weld force tests resulted in the figures in Table 18 for 0.4% C titanium and Type 302 stainless.

TABLE 17

EFFECT OF WELD FORCE AND WELD TIME ON STAINLESS STEEL WELDS*

WELD TIME (CYCLES)	BREAKING LOAD - POUNDS								
	WELD FORCE - 800#			WELD FORCE - 1400#			WELD FORCE - 2000#		
	SHEAR	TENSION	T/S	SHEAR	TENSION	T/S	SHEAR	TENSION	T/S
6	2190	1540	0.70	2230	1860	0.83	2160	1550	0.72
12	2460	1990	0.81	2530	2060	0.82	2490	1950	0.78
18	2400	1950	0.81	2430	1720	0.71	2670	2050	0.77
24	2690	1700	0.63	2830	2130	0.75	2710	2000	0.74

* TYPE 302 STAINLESS STEEL - C.R. BRIGHT FINISH
0.040" TH. - 90 ROCK. B

TABLE 18

WELD STRENGTH AVERAGE SUMMARY *

PROPERTY	MATERIAL	
	TITANIUM 0.4 %C	STAINLESS TYPE 302
TENSION-SHEAR (LBS)	2145	2482
TENSION (LBS)	552	1875
T/S	0.26	0.75

*AVERAGES – TABLES 13,14,15,17

Whereas negligible interface expulsion occurred in the titanium, considerable expulsion occurred in the stainless. Almost all welds at 600 lb. force in stainless exhibited expulsion and about 60% did at 1400 lb. At 2000 lb, expulsion occurred only at the highest weld time of 24 cycles. The higher strength (and stiffness) of the stainless obviously required higher forces to avoid expulsion.

In all time-force tests (Tables 13 and 14) the welder current setting was unchanged, and the weld current was measured by the "back-stop" ammeter method. The calculated weld current varied about plus or minus 10% (8200 to 10,100 amp), invariably measuring higher with the longer weld time. Undoubtedly the actual variation was substantially less than 10% in view of the inherent error in reading the ammeter and the high turn ratio multiplication factor.

(C) *Effect of Welding Current and Weld Time*

(*a*) *Procedure.* In the final phase of the investigation the principal variable was weld current. The balance of conditions were chosen from previous tests. The welding conditions were:

Material	0.4% C titanium, cleaned in CCl_4
Electrode contour	3 in. spherical radius
Weld force	1350 lb
Weld time	14, 16, 18 and 20 cycles
Weld current	Variable

Twelve current values were used from a low at which shear specimens did not pull a nugget to a value at which expulsion occurred. Two shear, two tension and one microscopic specimens were made at each of the 12 currents at each weld time.

(*b*) *Results.* Data were compiled in curve families, only two of which are included, representing selected weld current and weld force from the analysis of all the curves. A weld time of 18 cycles with a current of 11,000 amp was selected as providing the highest weld strength consistent with other acceptable weld qualities.

Figure 5 indicates weld characteristics as a function of current with a weld time of 18 cycles, and Fig. 6 shows characteristics as a function of weld time at 11,018 amp. Figures 5 and 6 indicate that the increasing weld current affects weld characteristics in the following ways:

1. Tension-shear strength and tension strength attain a maximum at approximately 12,000 amp with a weld time of 18 cycles.
2. Penetration of the weld nugget, electrode indentation and nugget diameter increase.

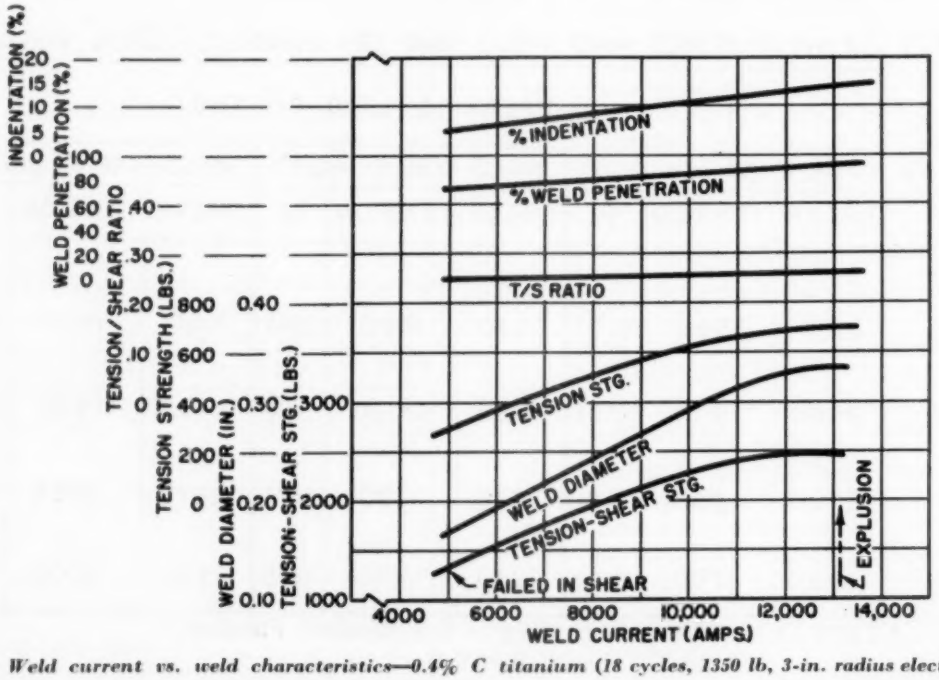


Fig. 5 Weld current vs. weld characteristics—0.4% C titanium (18 cycles, 1350 lb, 3-in. radius electrodes)

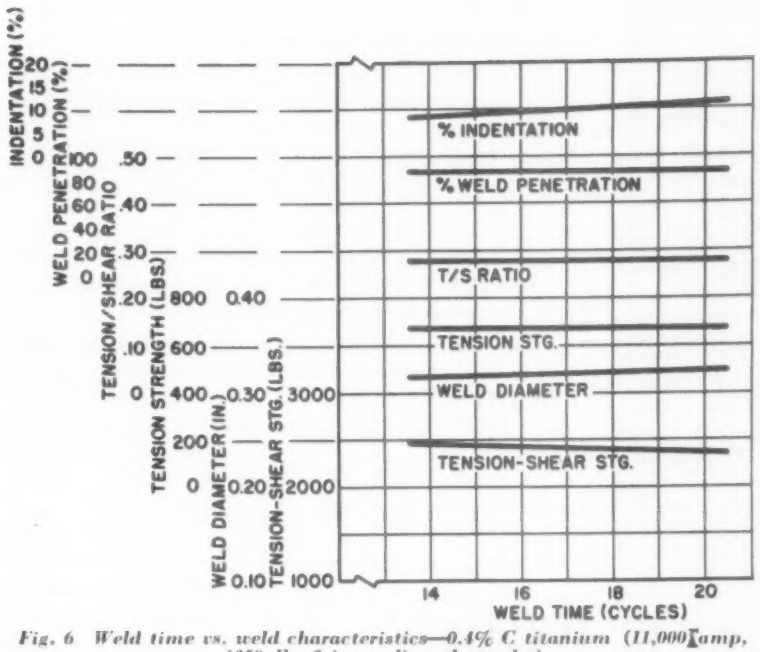


Fig. 6 Weld time vs. weld characteristics—0.4% C titanium (11,000 amp, 1350 lb, 3-in. radius electrodes)

3. The tension/shear ratio is not affected.

Increasing the weld time from 14 to 20 cycles increases indentation and nugget diameter slightly. It had little or no effect on the tension/shear ratio or weld penetration. Weld currents to approximately 11,000 amp cause the strength properties to increase rapidly and higher currents had little further effect.

X. RANGE AND OPTIMUM VALUES OF WELDING CONDITIONS AND RESULTANT WELD PROPERTIES

Spot welds of good strength, quality and consistency can be obtained in the 0.038-in. thick 0.4% C titanium in the "as received" condition using welding conditions listed in Table 19.

TABLE 19
RANGE & OPTIMUM VALUES OF WELDING CONDITIONS *

CONDITION	RANGE	PROBABLE OPTIMUM
WELDING CURRENT (AMPS)	8000-12000	11000
WELD FORCE (LBS)	1300-1400	1350
WELD TIME (CYCLES)	14-20	18
ELECTRODE MATERIAL	MALLORY 3	MALLORY 3
ELECTRODE CONTOUR	3,6,&10" RADIUS	3" RADIUS

* BASED ON HIGHEST PRACTICABLE WELD STRENGTH.

The conditions listed in Table 19 were found to be entirely suitable for welding the 0.1% C and 0.6% C, and Table 20 lists weld properties in all three titanium-carbon alloys welded under the conditions indicated as optimum.

Welds are characterized by high shear strength, ductility decreasing with increasing carbon content, complete absence of porosity, no cracks and expulsion, no sticking of electrodes and comparatively high weld penetration. Welds in the low carbon grade fracture with a ductile tear, while in the highest carbon grade a brittle fracture occurs by shattering or splitting of the titanium.

XI. CONCLUSION

From the results of the investigation the following conclusions may be drawn:

TABLE 20
WELD PROPERTIES USING OPTIMUM CONDITIONS *

PROPERTY \ ALLOY	0.1%C	0.4%C	0.6%C
MATERIAL THICKNESS (IN)	0.043	0.038	0.047
TENSION-SHEAR STG. (LBS)	2586	2375	2794
CROSS-TENSION STG. (LBS)	991	755	425
TENSION/SHEAR RATIO	0.38	0.27	0.15
WELD NUGGET DIA. (IN)	0.30	0.32	0.32
WELD PENETRATION (%)	93	84	82
INDENTATION (%)	12	10	10
TYPE FRACTURE	DUCTILE	FAIRLY DUCTILE	BRITTLE

* CONDITIONS FROM TABLE 19

1. The surface contact resistance of the "as received" titanium is sufficiently low and uniform to permit reasonably good weld consistency.
2. Contact resistance is appreciably lowered and made more uniform by mechanical or chemical cleaning with attendant improvement in weld strength consistency.
3. "Hydride" cleaning requires further study to avoid high contact resistance and possible impairment of weld ductility.
4. There is no apparent interaction between cleaning method and carbon content.
5. Weld strength is not affected by the direction in which the sheet is rolled.
6. Welding causes slight increase in hardness in the weld nugget and heat affected zone.

7. Weld shear strength is unaffected by carbon content.

8. The weld ductility is adversely affected by increasing carbon content.

9. Weld shear strength in titanium carbon alloys is comparable to those obtainable in stainless steel (18/8) although weld ductility is considerably lower even in the lowest carbon grade.

10. Variations in electrode contour between 3 and 10 in. do not appreciably affect weld strength or other properties.

11. No interaction occurs between electrode contour and weld force.

12. Weld ductility (tension/shear ratio) is little affected by variations in welding conditions.

13. Titanium carbon alloys are less susceptible to interfacial expulsion than is stainless steel.

14. Weld penetration is always relatively high in the range of conditions producing an acceptable shear strength.

15. The three grades of titanium carbon alloy may be satisfactorily welded over a rather broad range of welding conditions.

16. Single phase A C welding equipment of standard design and of good quality and control is satisfactory for spot welding titanium carbon alloys.

17. Range and optimum values of welding conditions have been established and weld properties reported.

The Human Element in Welding

There is need for young welding engineers who understand the use and application of acceptable welded designs. This calls for a high type of public relation activities

by Richard C. Wiley

THE situation today in the education of engineers for welding can be likened to a bewildered pedestrian standing on a busy corner in a strange city wondering which street to take in order to arrive at his objective. He must move in some direction or stand on the corner forever, but which way is not certain.

In dealing with the human element in welding, it is in this discussion that we hope to look into some of the choices in direction that welding education now has before it.

Investigation shows that the welding field does not attract a sufficient number of graduate engineers with a knowledge of welding processes and design to fill the growing demands of the industry. This is not only today's problem but in the face of new metals and other technological advances now on the horizon, it poses an even more serious problem for tomorrow. Manpower shortage appears to be one of the chief retardants for welding now and in the future.

Richard C. Wiley is Head of the Welding Department of the California State Polytechnic College, San Luis Obispo, Calif.

Presented at the AWS National Spring Meeting, Houston, Tex., June 16-19, 1953.

One example of the situation comes from a large western builder of refineries, chemical plants, power plants and pipe lines. This company finds itself badly in need of young engineers who can produce good welded design. They need men who understand the use and application of various welded designs—that is, acceptable, buildable design. More welding could and would have been used throughout this company's entire operations this year if there had been available an adequate supply of engineers and detailers.

This company is not alone in its predicament. There are hundreds of small fabricators in the west and southwest who do not have the services of engineers with a reasonable background in welding. Think also of the problem of the many one-man shops which rely almost entirely upon equipment suppliers for expert advice. This puts the supplier in the position of consultant—a position not necessarily advantageous to either party.

Two questions were put to numerous fabricators and construction companies, both large and small, as follows:

1. From your experience and position in the metal-fabricating industry, would you say that more engineers trained for welding are needed?

2. If a greater number of engineers with knowledge of good welding design and operations were available,

would your organization engage in an exhaustive study of your manufacturing or construction operations to determine the further economical possibilities of using welding over other methods of metal jointery? This would include not only manual arc welding, but the higher speed processes such as inert-gas-shielded, automatic submerged melt and resistance welding of heavier materials.

Responses indicated that many large organizations are studying new designs and the application of welding to improve products and reduce costs. Welding research laboratories, committees composed of men from various operational parts of the plants, evaluation of current technical literature, and in-plant training services seem to be the extent of the present methods used for looking toward design improvement and expansion of operations. Smaller organizations are unable to maintain these more desirable programs of investigation. They are forced to rely upon the abilities of their limited engineering staffs.

It is unfortunate to note that many of our major engineering colleges are presently directing the emphasis of their programs toward supplying men for only the largest industrial organizations. This is commendable as far as it goes. But to aim a program at 50 or even 500 organizations, when there are 5000 or even 10,000 smaller fabricators and contractors badly in need of engineering services, certainly can be considered shortsighted.

The smaller organizations cannot afford a large training and orientation program before the young engineer becomes a producer.

In both large and smaller organizations the survey showed that there is a marked shortage of good welding designers and detailers. Further, that this shortage curtails application of the various welding processes.

Enrollment in engineering schools has been dropping since 1949, when the peak occurred. It has been predicted by educators that this shortage will become even more acute in the next few years.

The exact number of new engineers that will be required each year cannot precisely be determined. However, on the basis of available information, the Department of Labor's Bureau of Statistics gives a rough estimate as follows: "The average annual demand during the prolonged partial mobilization would probably be at least 30,000."

This figure takes into account replacements for those who die or retire and would provide about 21,000 engineers annually to fill new jobs and occupy positions vacated by persons leaving the profession for other lines of work.

Translate this into the number of freshmen needed. The lowest practicable number of first-year college men and women that will be needed annually to maintain the flow of new graduates into the engineering profession required over a period of years by our defense economy is placed at an estimated 40,000 to 60,000 freshmen.

Henry H. Armsby, U. S. Office of Education's Associate Chief for Engineering Education, reports that unless conditions change sharply the number of engineering graduates—which stood at 52,071 in 1950—will fall to about 17,000 in 1954 and possibly even lower if withdrawals of college-age men for military service are included. If normal attrition rates prevail the total number of engineering graduates will not reach the estimated need of 30,000 per year until about 1965, in his opinion.

One of the important welding processes found in everyday fabrication is submerged melt welding

Cost, operation, application and limitations of this process should be considered thoroughly. The actual operation of the machine is secondary to the application of the process. Students interested in sales-service find considerable use for the knowledge they gain from study and analysis of this equipment.

In many instances, large organizations who three or four years ago selected only the few top engineering students are now asking for the entire graduating engineering class to take care of their expanding operations. The welding industry, because of its lack of a long established and traditional background compared to mechanical, electrical or civil engineering finds itself in a doubly difficult position for obtaining "new blood."

The manpower shortage and the subsequent curtailment in the possible use of welding are problems which should be of considerable concern to all of us in the welding industry.

How can the welding industry attempt to meet these problems? Some possible avenues of approach would seem to be indicated in expanding public relations, using engineering personnel to more advantage, making in-service plant training count, and calling upon education to a greater extent.

Welding suffers from a lack of prestige in the minds of the average lay person. Sadly enough the average engineering educator is hardly any better informed.

Even the management of many major metal fabricators does not recognize the important part the work of a welding engineer plays in the company's ultimate profit

Inspection is an important part of engineering for welding

Too often it has been placed too far down the list. Actual physical contact with the tools of the inspector and actual know-how use of these tools should be provided for students.

and loss statement. Too often welding is viewed simply as a nontechnical problem to be solved by the welding operator.

Public relations activities—that is the imparting of information about the industry—must not be confined alone to possible clients and users of welding. The industry's public relations should include the big job of educating the educator, the student, and the prospective student. To these persons welding means making a lot of sparks and fire, signifying almost nothing. Realization of the engineering and scientific background required to keep these sparks alive is almost totally lacking.

For example, how many high school counselors, who have considerable influence in guiding young persons into various fields of endeavor, know what the welding industry has to offer? How many college advisers and young engineering students themselves know much more? In plain words, the industry must use all forms of public relations media to "sell" welding. The result will be healthful for all—equipment manufacturers, fabricators, engineers and the public.

The welding industry needs to tell its story. It must show what it has to offer and arouse an interest in the young engineering student. Perhaps the best overall means of covering this subject would be the production of a 16-mm sound motion picture film beamed toward the freshman engineering student. It is he who is still in the process of casting about for a useful place to occupy in our industrial civilization. I submit this proposal to the AMERICAN WELDING SOCIETY, which is in the position to coordinate the production of such a film. Various interested welding manufacturers, educators and others also could be called upon to participate.

Another means of making engineering students aware of the opportunities and challenges in this field is the career-bulletin type publication made available to placement directors, guidance and counseling workers and engineering students. Nowhere in such material has the welding industry been mentioned except at the operator level. Even here the write-ups are at least 20 years outmoded.

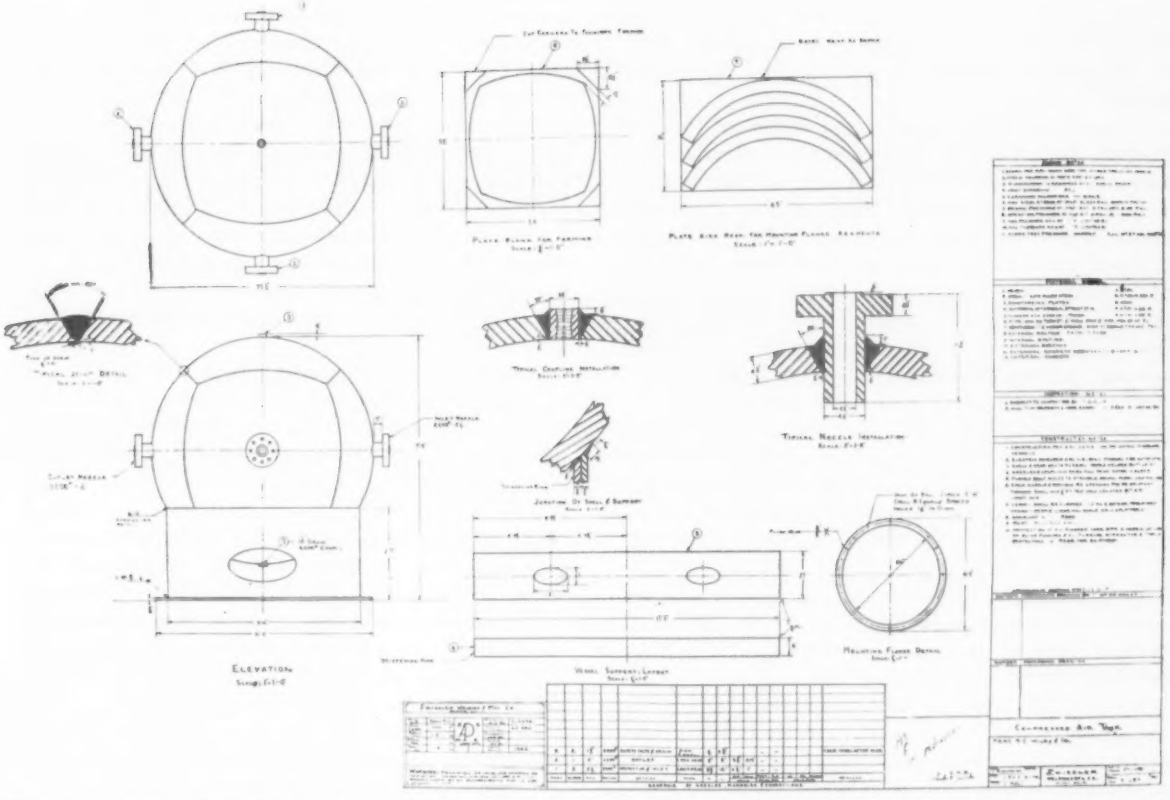
If the welding industry cannot tell its story to students who are entering engineering, how can it expect to gain qualified personnel?

A second avenue of approach lies in using available engineering manpower to its greatest productivity. Unfortunately, many engineering graduates are kept in routine testing and drafting jobs which might be filled by less technically trained personnel. In numerous plants there are men who have not had the benefit of much formal education, but who are eager and capable of making advancement toward a more satisfying goal in their work. Engineers so freed from routine jobs might well spend time in analyzing and solving more critical problems. In a field such as welding which has relatively so few available engineers, every encouragement and opportunity must be given to men with good technical background and who already are working in the industry. In-service training has much to offer as an approach to relieving some of the shortage of well-trained personnel for welding.

As an example of this in a western plant employing

Plant and job site visits should be carefully planned to bring the student in close relationship with actual operations

A chance to talk to shop personnel, foremen and supervisors about operations, problems and equipment can be afforded students.



This is a typical working drawing layout of a 6-ft OD spherical pressure vessel designed as a student project and in accord with the API-ASME code

Details of nozzle entrances, plate blank arrangement, skirt mounting, flange nesting and other pertinent data incident to construction and installation are shown.

approximately 30 welders who deal primarily with repair and maintenance work, an in-plant training program recently has been organized. One afternoon a month, on company time, various subjects are presented to the welding personnel. For the past three months titles of the welding education presentations have been as follows:

1. Electrode selection and evaluation.
2. Weld inspection by X-ray and what it means to the welding operator.
3. Weldability of low-carbon and low-alloy steels.

These discussions were presented by the faculty of a nearby engineering college. Also invited were other interested engineers within the company.

There are those men who lack formal engineering education, but who nevertheless are good material for many areas. Welding, because of its growth more or less from a craft to a science, has drawn heavily upon such persons in the past. A large number of these men have contributed greatly. People in welding know that the gilded letters of "graduate engineer" mean little in themselves. What the individual can do means everything. Let us not forget to develop this important source of manpower.

The welding industry badly needs more personnel with greater knowledge, both scientific and practical. Research is providing the fabricator with much new and complicated equipment. Few are the plant engineers who fully appreciate all applications. Few, too, are the operators who can use such equipment without further training.

Sales of welding apparatus and equipment offer a challenge as well. The greatest number of sales calls that are made are directed toward companies which already have determined their welding requirements and are using the process extensively. The average salesman is doing little but securing a known amount of business. Ask any welding salesman how many nonusers of welding he has called upon lately. More sales time should be spent with the nonwelding or infrequent users in the metal-working industry. This should do much to expand the market. Sales engineers might well examine carefully the metal-joining processes being used and give the client a concise written report of probable savings by the use of the applicable welding processes.

The many weird fables, tall tales and general "hocus-pocus" surrounding much of the technique at the operator level is but another indication of this need. Such lack of knowledge should be dispelled by a continuing effort on the part of engineers, sales personnel and others

who encounter it. A very large book of welding misinformation could be compiled in an amazingly short time by anyone interested in pursuing this fascinating subject.

As to engineers for welding, an in-service training program for continued improvement is of great significance to the individual as well as to the organization. Too few plants of medium size capitalize upon this means of developing full use of manpower. Meetings in which problems are shared and solutions evolved can be carried on in both large and small plants with adaptations for each. Also, the tendency toward only "high-level" meetings must be supplemented by equally valuable get-togethers on various other levels. Many production, engineering and sales problems can be tackled in such meetings. The values are twofold: possible solutions often are made available in the viewpoints presented and the sense of belonging to a team where actual participation is a fact adds up to good industrial psychology. Enthusiasm comes from participation. A good job must have both to succeed. Some of the means to be explored in in-service training would appear to be staff meetings within the plant, tours of all the various parts of the plants, university extension courses, attendance at technical society meetings, institutes, and congresses and expositions such as the present one. If men have the desire to learn more about welding it would be shortsighted of industry not to encourage them. Where industry is willing to help a man advance himself, it would be equally shortsighted for the individual not to take advantage of opportunities presented.

Welding must call upon education more and more to fill its manpower needs of the future. Education can aid greatly provided it is awakened to the requirements and needs of the industry. Education must be assured, before it admits newcomers to its curricula, that the field has a reasonable life expectancy ahead. Also, that it is of value to society and that its graduates will find acceptance for years to come. Education shuns away from the specializations of the moment—it has to be "in business" on a long-range basis. Seemingly, welding, as we know it, can answer these prerequisites.

Educators, as shown by the AWS Education Survey of 1951, are relatively timid to enter immediately into uncharted ventures such as those offered by the welding field. Today these educators speak only of integration and of basic engineering fundamentals when welding engineering is mentioned. It may be that this is a hopeful sign. As many persons know who realize the full content of the welding field, education for mere specialization is not that which is needed, but rather broad understanding within this broad field must be developed. Perhaps every major engineering branch should look anew at the charge of "specialization." This word has come to carry an emotional load rather than a rational one. Too often educators use it to apply to what the other fellow is doing which they do not like. Broad fundamentals, as important as they are, need a dash of specialization to relate them to present industrial practices.

One possible solution would appear to be the adoption by engineering colleges of the so-called senior-year option in engineering for welding. The natural root for this option arrangement would be found in the mechanical or metallurgical departments. Students trained in these branches would find transition into the welding field a most desirable addition to their basic engineering program. The charge of lack of versatility of the straight welding engineer would not apply to such men. If a career in welding should prove unsuitable for the individual, he would not be penalized by shifting over to the mechanical engineering field. There would be no loss to himself, the profession or society.

It has been expressed by those concerned with the advancement of welding that our colleges must establish welding courses and they must graduate qualified engineers for this field. An estimate by these men indicates that some 95% of welding engineers—so classified today—were educated in another field of engineering and drifted into welding for one reason or another. When colleges accept welding as an established engineering science, so will management.

Few are the universities or colleges that have accepted the challenge in education which welding offers. These few, however, are producing men for the welding industry and have encountered wide acceptance and much satisfaction in their programs. At California State Polytechnic College, a senior-year option course in welding within the framework of the mechanical engineering department has been in progress for the past three years. Graduates have entered the field in design, sales-service, production and inspection. Perhaps it is yet too early to know if this program is a completely adequate one. Improvements and advice from industry are continually being sought.

The Brazing of Titanium

Methods of making brazed joints, development of fluxes and preliminary investigations on recrystallization welding

by N. A. DeCecco and John M. Parks

Abstract

Methods have been developed for brazing commercially pure titanium with an oxyacetylene torch, by electric resistance heating and in a furnace using an inert gas atmosphere of commercial purity. Torch-heated single-lap joints were made which had a high shear strength when tested in tension. Joints brazed with fine silver broke with a ductile shear fracture whereas joints made with other braze metals broke with a brittle shear fracture. Resistance heated lap joints were made with sufficient strength to break the titanium in tension outside the lap area.

The surface films which form on heating were studied by reflected electron-diffraction techniques. Fluxes were developed for removing these surface films; they incorporated metal depositing reagents such as AgCl, Cu_2Cl_2 and $MnCl_2$. When fused on a titanium surface, these fluxes react with the titanium, undercut the oxide surface, and deposit a protecting metal film in a chemical replacement type of reaction.

An approximate titanium-silver phase diagram was constructed; it provided a metallurgical understanding of metallographic structure of joints brazed with fine silver and verified the existence and ductility of the intermetallic compound TiAg, which was discovered in the course of this investigation.

An exploratory investigation for welding commercially pure titanium and a 7% manganese-titanium base alloy by recrystallization welding produced joints with shear strengths equivalent to the shear strength of the annealed titanium base metal.

Several factors must be taken into consideration when brazing a highly reactive metal like titanium. Finding the proper brazing alloy and flux is most important. Associated with this problem are wetting and metal flow, selection of method of heating, and metal protection during brazing.

A large number of metals react with titanium to form brittle intermetallic compounds. Such metals when used as brazing alloys limit the use of brazed joints to those designs wherein only shear loading is of significance. Hence, it is important to select a brazing alloy which is free from brittle intermetallic compound formations. This was not a simple problem at the time this work was started; for example, the phase diagrams of many of the titanium systems and the properties of the various intermetallic phases were not known.

The formation of nonmetallic films on titanium, when it is heated, prevents wetting and metal flow. The exact nature of these films was not known at the outset of this work; hence, studies of them were made. Except for furnace brazing in inert atmospheres, a brazing flux to remove them is essential. As only one commercial flux proved to be of value, a flux development program was also necessary before evaluation of brazing alloys and methods could be undertaken properly.

The following paper discusses these problems, first by a consideration of the metal systems of interest in brazing titanium, then by a review of titanium surface films and flux development, and finally by a presentation of the results of testing lap brazed joints. In this work, brazing studies were limited to commercially pure titanium although some recrystallization welds were made in both commercially pure titanium and the 7% manganese alloy.

N. A. DeCecco is Research Metallurgist at the Armour Research Foundation of Illinois Institute of Technology, Technology Center, Chicago, Ill., and **John M. Parks** is Manager, Metallurgical Process Division, Air Reduction Research Laboratories, Murray Hill, N. J.

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BRAZING METALS

MANY types of brazing metals are available commercially; and information had been obtained by those who had tried some of these metals for brazing titanium.[1, 2] A survey was made to select those metals most likely to be suitable as brazing alloys.

Pure metals were selected for initial evaluation; this permitted an appraisal of the individual alloying characteristics of each and provided data which later could be used to interpret what occurred when each metal was used as a brazing alloy component. The use of pure metals also provided an opportunity to record metallographic structure and interpret it in terms of the phase diagrams.

Melting point, shear strength, and phase relationships[3] became the major factors which lead to the selection of silver, aluminum, copper and gold base alloys for further study. Of these, only silver proved to be of significance; gold was not adequately studied and, therefore, should not be ruled out as a satisfactory brazing metal.

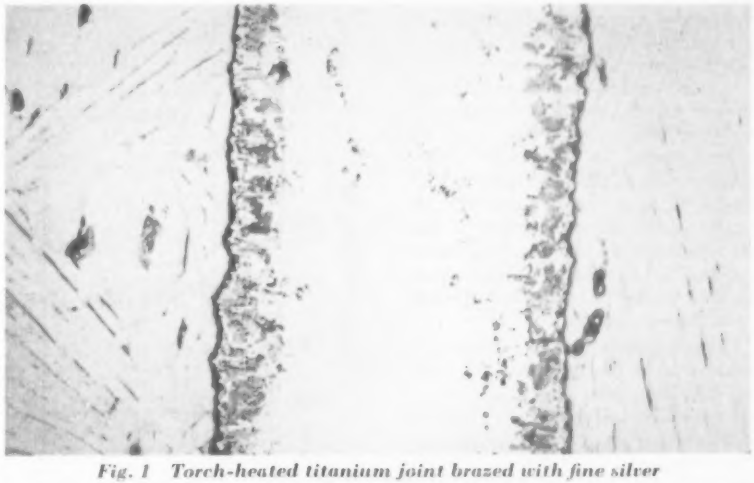


Fig. 1 Torch-heated titanium joint brazed with fine silver

The black line at the brazed metal-titanium interface is due to etching relief. The gray columnar constituent is the intermetallic compound TiAg. The light-colored metal in the center is silver.

First etchant: 20% HF + 20% HNO_3 + 60% glycerin. Second superimposed etchant: 35% NH_4OH + 35% H_2O_2 + 30% H_2O. Magnification × 500.

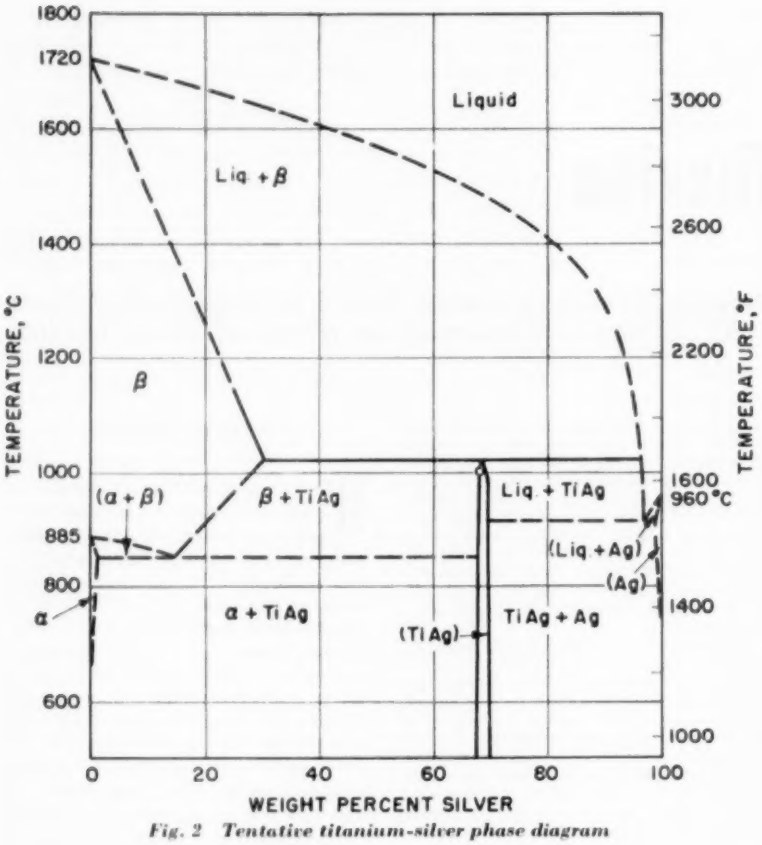


Fig. 2 Tentative titanium-silver phase diagram

Silver

When fine silver was used as a brazing alloy, a strong ductile joint was produced. This was unusual in view of the presence of an intermetallic phase, which was subsequently detected in the microstructure, Fig. 1.

Unpublished information indicated that silver and titanium did not alloy, confirming a previous investigation[4] which claimed that titanium and silver were insoluble in both the liquid and the solid state. However, metallographic studies of brazed joints revealed an intermetallic phase, and bend and tensile shear tests of brazed joints indicated that this phase exhibited ductility. To substantiate these findings, a quick survey of the titanium-silver system was undertaken. A number of titanium-silver alloys were made, heat treated, and examined metallographically to establish the basic nature of the titanium-silver phase diagram, Fig. 2.

These determinations substantiated the existence of an intermetallic phase, established its composition as TiAg, and verified the conclusion that such a phase was ductile. An ingot of the compound composition was melted, and the cast metal was cold compressed 70% with only slight edge cracking. This high ductility is unique; it is the only such compound in the titanium binary systems discovered to date which has good ductility at room temperature. The only other ductile titanium compound is TiAl (or γ phase); compared with TiAg, this latter compound is only slightly ductile.

X-ray studies of the TiAg compound[5] established its crystal structure as an ordered face centered tetragonal lattice with atoms of one metal at (0, 0, 0) and ($^1/_2$, $^1/_2$, 0) positions and atoms of the other metal at (0, $^1/_2$, $^1/_2$) and ($^1/_2$, 0, $^1/_2$) positions. The dimensions of the unit cell were determined as: $a = 4.094$ Å; and $c = 4.049$ Å; and $c/a = 0.993$.

The temperature difference between the melting points of pure silver and the silver-TiAg compound accounted for the good spreading power of silver into the joint and the low undercutting which was observed. On melting, silver flowed into the joint and the TiAg compound formed at the titanium-silver interface by solution of titanium in the molten silver; subsequent erosion was halted by the slow rate of diffusion of titanium through this solid compound layer and by the rapid saturation of the melt with small quantities of titanium.

When the joint was brazed at a temperature above the melting point of the TiAg compound, the microstructure of the brazed metal was a mixture of retained β-titanium and TiAg compound, and under cutting became more significant.

Copper

The titanium-copper binary system, Fig. 3,[6] has several compounds which are brittle. This brittleness was demonstrated by fusing copper on a titanium surface at a temperature slightly above the eutectic temperature. The resulting alloy was very brittle; it fractured when the titanium sheet was bent a few degrees. This tendency was consistent with all subsequent findings; all joints brazed with copper or copper-bearing alloys broke with a brittle fracture.

The broad temperature range between the melting point of copper and that of the $TiCu_2$-Ti_2Cu_3 eutectic indicates that any attempt to melt pure copper and flow

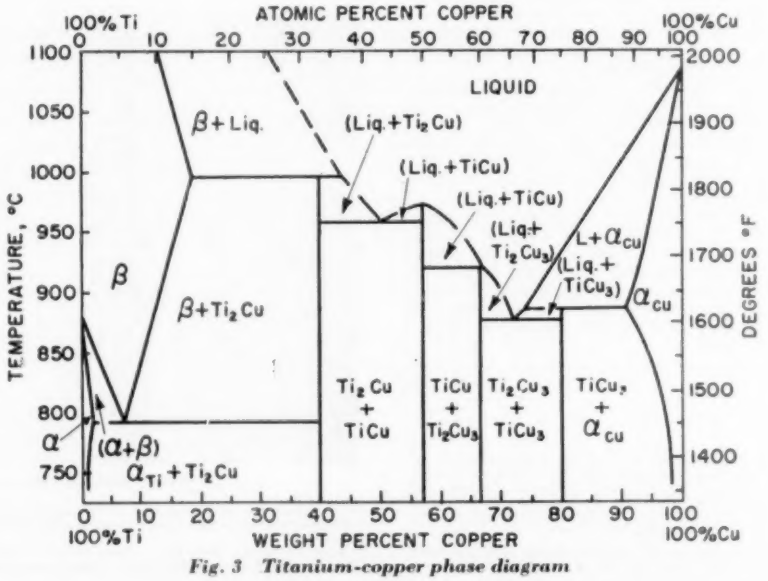


Fig. 3 Titanium-copper phase diagram

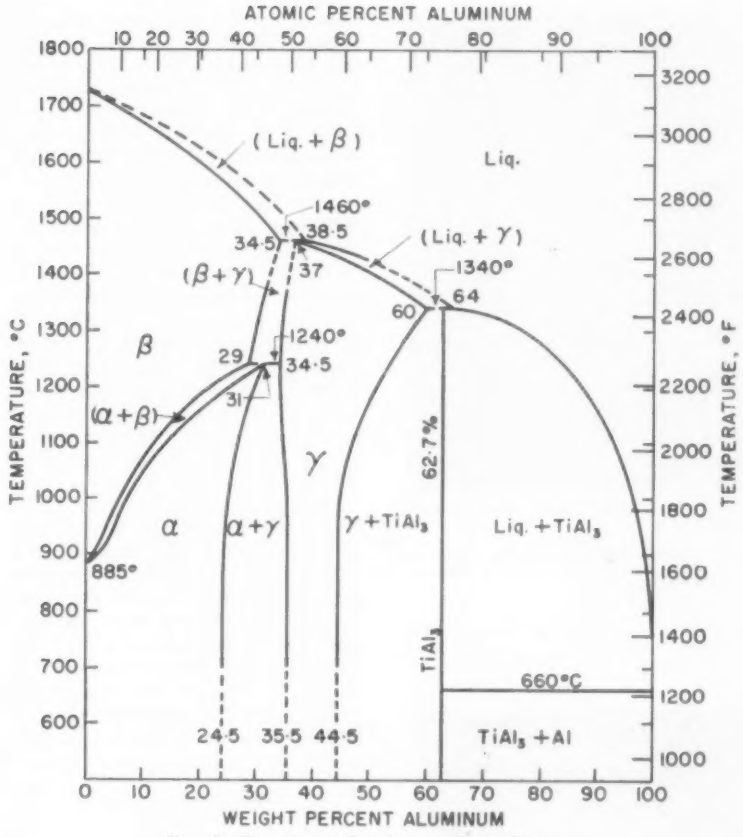


Fig. 4 Titanium-aluminum phase diagram

it into a titanium capillary joint should result in severe erosion and undercutting. Such proved to be the case. The highly superheated melt, which is produced on melting copper, and its subsequent excessive solution of titanium—alloys containing 60% titanium are possible—causes extensive melting of titanium and loss of metal contour. This tendency excludes the use of pure copper for brazing titanium.

Aluminum

A consideration of melting points indicated aluminum would be an ideal brazing alloy; however, the titanium-aluminum phase diagram[7] shows the existence of two intermetallic phases: $TiAl_3$ and TiAl, Fig. 4. On brazing, the $TiAl_3$ phase formed a thick layer, Fig. 5, and also was rejected from the melt as coarse crystals; unfortunately this compound is quite brittle: all joints containing it failed in a brittle fracture.

The TiAl compound also formed a layer in the brazed joint. This compound is known to have some ductility at room temperature; hence, as would be predicted, fracture rarely occurred in this region of the joint. The relatively high melting point of the compound $TiAl_3$ and the relatively low solubility of titanium in molten aluminum at temperatures near its melting point indicate that aluminum should exhibit good spreading power without excessive erosion and undercutting. Such also proved to be the case.

If very rapid brazing times and relatively low temperatures were used, it was possible to make brazed joints using pure aluminum without creating excessively thick compound films; as a result, it was possible to achieve some joint ductility. Lap joints occasionally broke with a ductile fracture, particularly when thick joint spacings were used. Thick joints have adequate aluminum to permit considerable shear prior to stressing severely the brittle surface films next to the titanium, Fig. 6.

SURFACE FILMS ON TITANIUM

During the course of the investigation, a comprehensive study of titanium surface films was published.[8] Inasmuch as research under way completely supported this work, extensive further investigations on titanium surface films were not necessary.

Electron diffraction studies revealed that only oxide (rutile) formed on titanium when it was exposed to air at elevated temperatures for short times. At room temperature an invisible film formed; it was detected indirectly by observing the disappearance of the electron diffraction pattern of titanium. Electron beams have poor penetrating power; hence when a thin film forms on titanium, the incident and reflected beams emitted are completely absorbed and the titanium diffraction pattern is lost. Calculations from electron diffraction observations lead to the conclusion that either the film which formed was essentially amorphous or, if crystalline, it was less than 100 Å thick.

Figure 7 shows a series of four electron diffraction photographs. Figure 7(*A*) is a photograph of a freshly abraded titanium

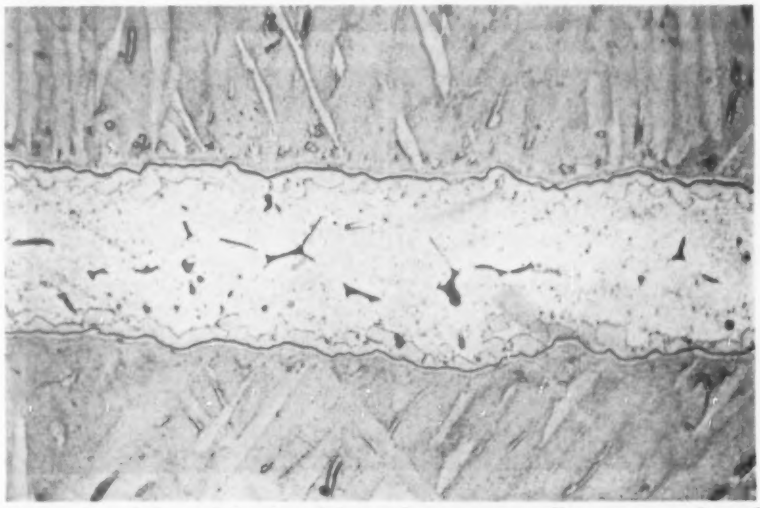


Fig. 5 A torch-heated titanium joint with a narrow capillary gap space brazed with 2S aluminum

The thick film at the brazed metal-titanium interface is the intermetallic compound $TiAl_3$. Unetched. Magnification × 500.

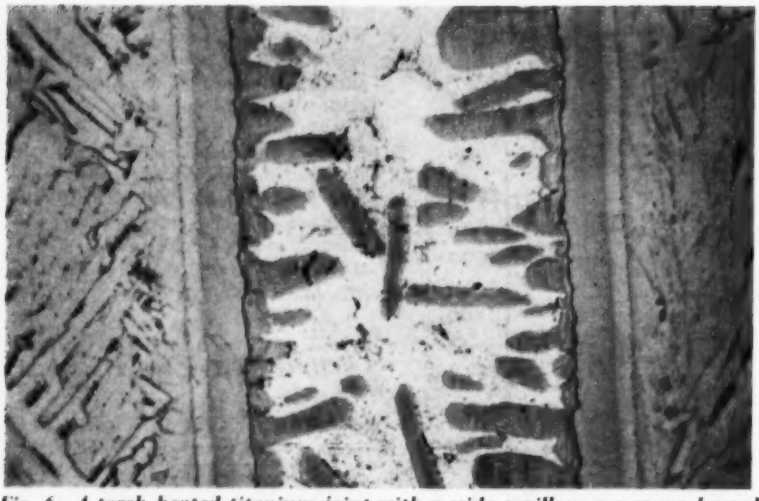


Fig. 6 A torch-heated titanium joint with a wide capillary gap space, brazed with 28 aluminum

The brazed metal matrix is aluminum. The massive globules are intermetallic compound $TiAl_3$. The two films adjacent to the titanium are Ti-Al phase and aluminum rich α titanium. Etchant: 20% HNO_3 + 20% HF + 60% glycerin. Magnification × 500.

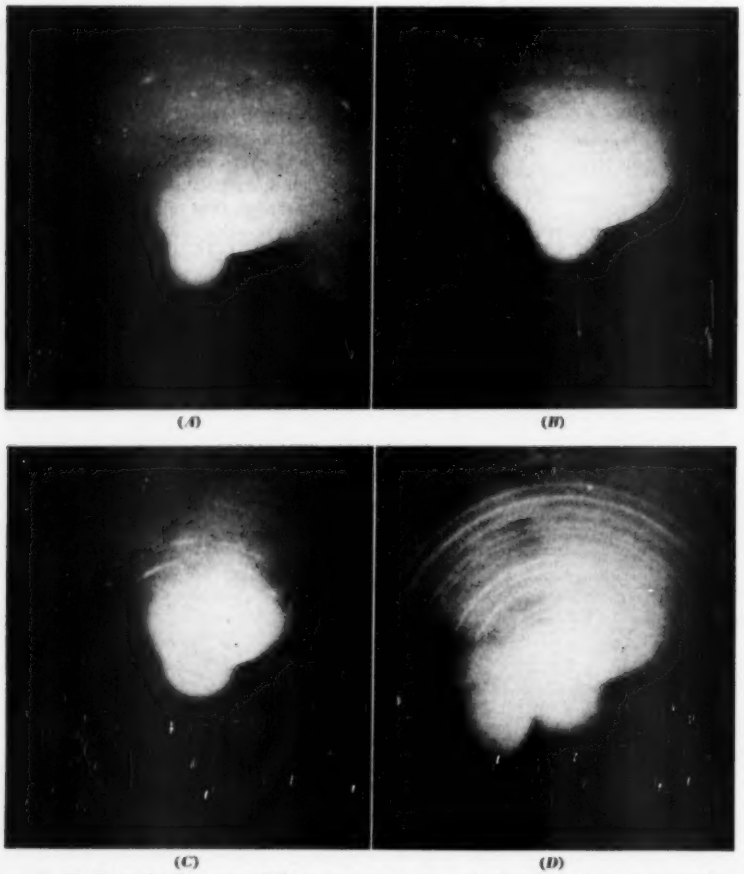


Fig. 7 Reflected electron diffraction patterns of abraded titanium surfaces

(*A*) A titanium diffraction pattern of a freshly abraded surface. (*B*) The same surface as (*A*) after exposure to air for five days. (*C*) Diffraction pattern of TiO_2 phase (rutile) formed on titanium heated in air at 450° C for 15 min. (*D*) Diffraction pattern of TiO_2 phase (rutile) formed on titanium heated in air at 850° C for 15 min.

surface; it shows the titanium diffraction pattern. Figure 7(*B*) is this same surface after standing in air for five days. On standing in air, the α-titanium pattern gradually disappears because of the tendency of the film that forms to absorb diffracted electrons. Heating for 15 min at 200° C will produce the same effect.

On heating for 15-min intervals, no diffraction pattern from the surface film was detected until temperatures of approximately 450°C were attained. At this temperature, the TiO_2 phase (rutile) pattern was observed, Fig. 7(*C*). As the temperature was increased further, this pattern becomes more pronounced. Figure 7(*D*) shows the pattern obtained for a film produced by a 15-min exposure in air at 850° C; higher temperatures do not produce a film which gives a stronger pattern.

In conclusion, films form on freshly abraded titanium surfaces when standing in air, and at brazing temperatures, rutile (TiO_2) films form. Experimental work showed that these films inhibited metal wetting and obstructed metal flow when titanium was heated in air without flux. Brazing fluxes were found to be essential, except for titanium that was abraded and immediately transferred into an inert atmosphere and brazed.

BRAZING FLUXES

All molten metals wet and flow on solid metal surfaces provided these surfaces are free from nonmetallic films. The purpose of a flux is to remove such films and provide a clean metallic surface upon which metal wetting and flow can occur. It is not always necessary to use a brazing flux to achieve metal wetting, but it is usually highly desirable.

For example, without flux, molten silver does not wet titanium immediately after fusion: it forms a globule which remains for some time before it suddenly breaks through the surface film and contacts titanium. Because of the formation of the intermetallic compound, TiAg, which provides a lattice movement to loosen titanium surface films and substitutes a TiAg film which is wet by silver, wetting occurs. However, spreading is slow; the extent and rapidity of this action depending upon the atmosphere surrounding the specimen.

On abraded titanium in a helium atmosphere, the wetting and spreading is rapid and satisfactory. In air, some type of flux is definitely required to assist the process so that it can be used to make a brazed joint.

In the case of copper, which forms a low melting point eutectic melt with titanium, once the surface is broken, severe undercutting of the base metal surface provides satisfactory metal contact and spreading occurs. However, this mechanism for providing wetting during brazing is un-

desirable because of the general erosion and loss of metal contour.

To date all brazing fluxes are operative because of one or both of the following functions:

1. Removal of the film by physical absorption or chemical solution.
2. Attack of the surface metal beneath the film, thereby loosening it mechanically so that it may separate from the titanium surface when the flux or metal covers it.

One commercial flux, containing considerable fluoride and capable of gaseous hydrofluoric acid attack, has been developed which is satisfactory for brazing low melting point, silver base alloys. However, it is not satisfactory for oxyacetylene brazing with fine silver or similar high melting point alloys because this flux has no mechanism for protecting titanium against severe oxidation at these higher temperatures. Accordingly, a new principle was applied to flux development; namely, electrochemical deposition of a metal protecting film. Using it, several new fluxes were evolved which were quite successful for brazing with all silver base alloys regardless of their melting temperature.

Molten chlorides—such as AgCl, Cu_2Cl_2, $SnCl_2 \cdot 2H_2O$, and $MnCl_2$—react with titanium surfaces, deposit metal films which protect the metal from subsequent oxidation, and release gaseous titanium tetrachloride which aids in disrupting nonmetallic films on titanium.

The use of pure salts, however, is not generally desirable because the higher melting points of metal depositing salts compared with salt mixtures. The higher the melting temperature of the flux, the greater is the degree of surface oxidation before the flux covers the surface and deposits a protecting film, and because the initial oxidized film is thicker the higher the temperature, the slower the effective action of the flux becomes.

A study was undertaken in which metal depositing salts were added to low melting temperature mixtures of salts, such as LiCl, LiF, KCl, KF, NaCl, NaF, $CaCl_2$ and CaF_2. The effectiveness of various combinations for silver brazing was evaluated by observing the flow after fusion of a ring, 1/4 in. in diameter, of fine silver wire, 0.047 in. in diameter, on a 1-in. square of ten mil titanium sheet which had been chemically cleaned in 10% HF solution. The titanium square was placed upon a 1/16-in. stainless steel sheet, Fig. 8, the silver ring positioned upon the center of the square and covered with the flux being tested. Heat was applied to the bottom side of the stainless steel plate with an oxyacetylene torch and heating maintained until the silver melted and flowed. If melting and flowing was instantaneous, the torch was removed immediately after melting; if melting but limited flow occurred, heating was maintained for an additional 5 sec to achieve the maximum metal flow likely to occur.

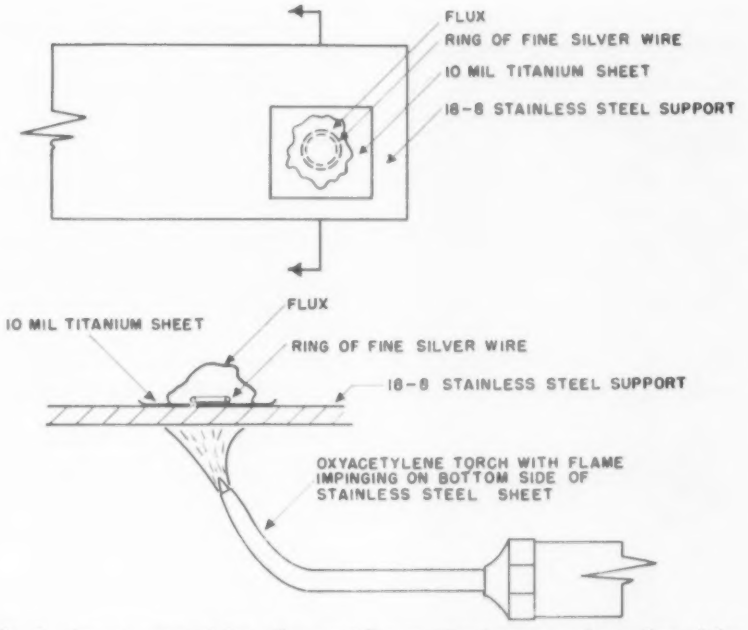

FLUX
RING OF FINE SILVER WIRE
10 MIL TITANIUM SHEET
18-8 STAINLESS STEEL SUPPORT
10 MIL TITANIUM SHEET
FLUX
RING OF FINE SILVER WIRE
18-8 STAINLESS STEEL SUPPORT
OXYACETYLENE TORCH WITH FLAME IMPINGING ON BOTTOM SIDE OF STAINLESS STEEL SHEET

Fig. 8 Arrangement of fine silver and flux on titanium to evaluate flow of fine silver using various experimental fluxes

After cooling, the titanium section was wire brushed to remove the blue-gray surface scale and pickled in 5% nitric acid for a short time to complete the cleaning operation. The effectiveness of the flux was estimated by the area covered by silver and the microscopic (45×) appearance of the flowed metal surface. Low-power microscopic observation was used to determine whether or not the flux had removed all of the surface film or had become entrapped in crater-like cavities as the molten silver advanced across the titanium surface. Generally the brazed metal-titanium interface was completely free from entrapped nonmetallic material; however, a limited number of fluxes exhibited this tendency and were accordingly rejected.

The following general observations were made from a large number of experiments:

1. To secure flow of fine silver on titanium using an oxyacetylene torch, the flux required a metal depositing reagent.
2. Silver chloride was the best metal depositing flux reagent for silver brazing.
3. Lithium salts were necessary reagents to use in conjunction with metal depositing fluxes.
4. $MnCl_2$ and Cu_2Cl_2 were poor metal-depositing reagents for torch brazing although they were quite successful in inert atmospheres.
5. $SnCl_2 \cdot 2H_2O$ in the flux caused silver to rapidly spread and then disperse; hence, it was not considered a satisfactory metal depositing agent for silver brazing. This action is believed to result from rapid wetting and alloying of the molten tin deposited surface by molten silver, oxidation of the titanium surface by oxygen absorbed in the silver and subsequent dewetting by silver on this oxidized surface.

The next stage in the evaluation of fluxes for brazing fine silver was to determine their ability to produce complete, uniform flow in lap joints consistently. By observing the fracture and shear strengths of a large number of brazed joints, six experimental fluxes were evolved; the composition of these fluxes and a commercial titanium brazing flux, which was analyzed as a part of this investigation, are tabulated in Table 1.

The same general procedure was followed for evaluating the proper fluxes for other brazing alloys. In general, metal deposition commences immediately after melting of the flux; if adequate protection is not given this film by the flux, this deposited film may be destroyed or become chemically removed prior to melting of the brazing alloy. For example, excellent copper films can be produced on titanium but they lose their ability to flow silver in an oxyacetylene torch-air atmosphere because of oxygen pickup.

Films of silver, copper and tin may be deposited from their fused salts in a helium atmosphere. These films can be successfully soldered by 60% Sn–40% Pb and 50% Sn–50% Pb solders when heated with a soldering iron using commercial noncorrosive soldering fluxes.

Silver films deposited, or attempting to deposit, from fluxes containing silver chloride and potassium hydrogen fluoride are attacked by the flux and immediately removed. Strong active agents must be avoided in flux formulation when electro-

Fig. 9 Assembly of metal strips, braze metal and flux in fixture for making a brazed lap joint

chemically depositing agents are used, otherwise the benefit of a protecting metal film is lost by attack and solution of this film as it forms.

OXYACETYLENE BRAZED LAP JOINTS

To evaluate the effectiveness of various brazing alloys and fluxes for lap brazing titanium to itself and to stainless steel, iron and aluminum, a fixture, Fig. 9, was made and a procedure established for brazing $^1/_8$-in. thick sheet using a $^1/_4$-in. overlap.

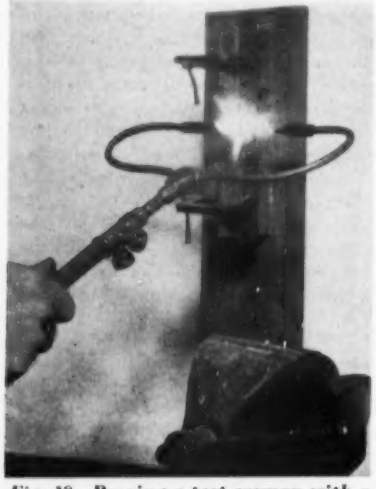


Fig. 10 Brazing a test coupon with a double tip torch

Table 1—Fluxes for Oxyacetylene Torch Heated Brazed Joints

	Composition—									
Flux	*AgCl*	Cu_2Cl_2	$MnCl_2$	*KF*	*KCl*	*NaF*	*LiF*†	*LiCl*	*NaCl*	$SrCl_2$
A	10	..	..	..	36.0	..	9.0	..	45.0	..
B	5	..	..	..	38.0	..	9.5	..	47.5	..
C	3	..	..	..	38.8	..	9.7	..	48.5	..
D	4	1	..	50	..	..	45.0	..	..	..
E	4	..	..	50	..	..	46.0	..	..	..
F	..	..	25	25	..	..	..	50.0	..	..
G*	..	1	..	46	..	2	..	50.0	..	1

* A commercial flux developed for brazing titanium. The dry composition was determined by a chemical analysis. The flux is 75% solids and 25% H_2O.

† Lithium chloride was found to be essentially equivalent to lithium fluoride in fluxes A, B and C.

Table 2—Commercial Braze Alloys Used in the Investigation

	Composition							*Solidus temperature,*	*Liquidus temperature,*
Alloy	*Ag*	*Cu*	*Zn*	*Cd*	*Ni*	*Mn*	*Pb*	° *C*	° *C*
A	50	15.5	16.5	18	..	..	..	627	635
B	50	15.5	15.5	16	3	..	..	632	688
C	72	28.0	..	..	..	..	..	779	779
D	85	..	..	..	..	15	..	952	960
Nickel brass	..	67.0	25.0	..	7	..	1	..	..

One-inch wide strips, 3 in. in length, were cleaned on the end to be brazed. Titanium was cleaned by sandblasting to remove surface scale and then pickled with 10% hydrofluoric acid solution; stainless steel, iron and aluminum were cleaned by filing. The specimens were mounted in the fixture, a $1^1/_2$-in. length of brazing wire placed upon the edge of the joint, and the entire edge covered with flux as shown in Fig. 9. Brazing was performed with a single gas torch by moving rapidly from one side to the other; later, a double tip torch was substituted for making test specimens, Fig. 10. With this torch, it was possible to obtain faster and more uniform heating. For all test specimens, the joint was brazed at a minimum temperature in as short a time as possible.

Fine silver and those brazing alloys shown in Table 2 were then evaluated using several different fluxes shown in Table 1. Shear strengths were determined for a nominal $^1/_8$-in. overlap joint having a small fillet by measuring the shear load and dividing this value by the actual sheared area of the fractured overlap surface.

All joints brazed with fine silver broke in ductile shear; their shear strengths are shown in Fig. 11. The structure of the "as-brazed" silver joint, Fig. 1, shows considerable shear in the silver after fracture, Fig. 12. This extreme metal flow in the silver solid solution is positive metallographic proof of joint ductility, and the limited flow, indicated by the deformation of the compound protrusions, is evidence

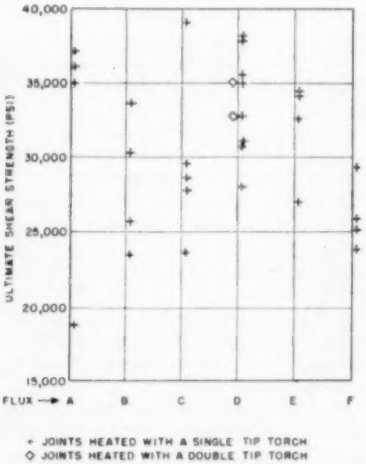


Fig. 11 Ultimate shear strengths of $^1/_8$-in. single-lap titanium joints brazed with fine silver using an oxyacetylene torch

Fig. 12 Brazed silver of a tested joint deformed by shear stresses

The titanium base metal is at the top; the brazed silver at the bottom and the ductile intermetallic compound TiAg at the titanium-brazed metal interface. First etchant: 50% HNO_3 + 50% H_2O. Second superimposed etchant: 20% HF + 20% HNO_3 + 60% glycerin. Magnification × 250.

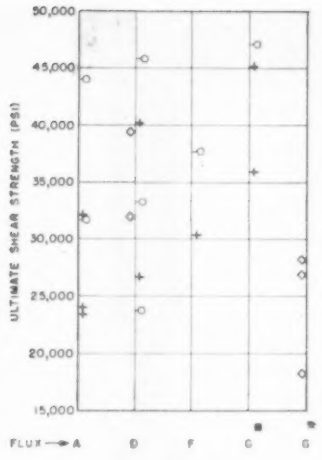


Fig. 13 Ultimate shear strengths of 1/8-in. single-lap titanium joints brazed with Alloy A using an oxy-acetylene torch

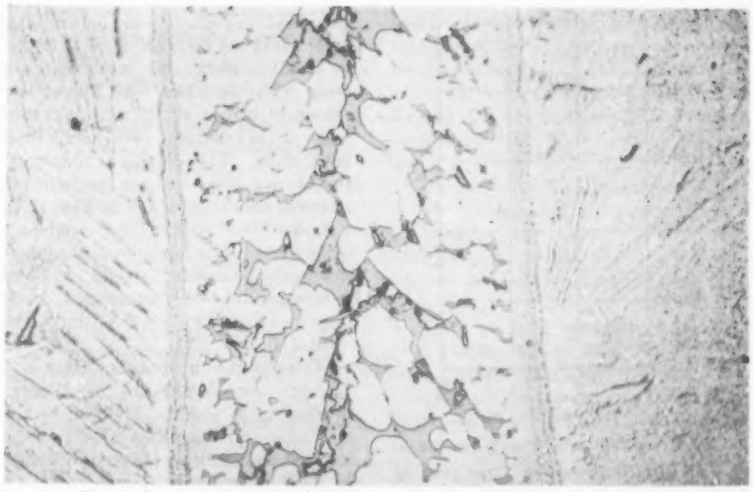


Fig. 14 Torch-heated titanium joint brazed with Alloy A. Etchant: 20% HF + 20% HNO_3 + 60% glycerin. Magnification × 500

of the ductility of the intermetallic compound, TiAg.

A 5/8-in. overlap was brazed to determine the effectiveness of flux A and fine silver to flow into a wide lap area. The specimens were positioned horizontally with a section of fine silver wire placed against one end of the lapped specimen and the entire area covered with flux. On melting, molten flux then silver were drawn into the capillary space by surface tension forces without the aid of gravity, when the joint was heated from the bottom side. The flux melted first, fed quickly into the capillary and served as an efficient heat transfer mechanism; then the silver melted and replaced the molten flux to produce a joint with completely filletted edges and a completely filled capillary space.

All joints brazed with the low melting point silver-copper-zinc-cadmium brazing alloy, Alloy A, broke with brittle shear fractures. The brittleness was attributed to the copper content of the alloy; copper forms brittle intermetallic compounds with titanium.

To estimate the influence of zinc and cadmium on joint brittleness, joints were brazed with 8.5% Zn, 91.5% Ag and 18.4% Cd, 81.6% Ag alloys. Although both alloys eroded titanium to a slight degree, joints made with each broke with a ductile fracture. On the other hand, joints made with 3% Cu, 97% Ag alloy broke with a brittle fracture. From this, it was concluded that the copper content of Alloy A was responsible for joint brittleness.

Alloy A can be used by hand "feeding" the alloy wire against a heated and fluxed joint; however, severe erosion of titanium occurs at the point of wire contact with the titanium.

The ultimate shear strengths of titanium joints brazed with Alloy A using various fluxes are shown in Fig. 13. Because of joint brittleness, shear strength varied to a considerable degree. Figure 14 shows a typical photomicrograph of a cross section of a brazed joint made with this alloy. The plane of fracture in this structure was not consistent; in most specimens, it was near the titanium metal interface. No detectable crystal shearing was found.

Alloy B, which is similar to Alloy A but contains 2% less cadmium, 1% less zinc and 3% nickel, did not wet titanium well; only a few joints were made where complete flow of brazing alloy into the 1/8-in. overlap could be achieved. This tendency was attributed to the presence of nickel but no metallurgical explanation was pursued. On hand "feeding" wire, erosion of titanium occurred at the point of feeding. The shear strength, of the few joints that were made are shown in Fig. 15; all broke with a brittle fracture.

The copper-silver eutectic alloy, Alloy C, caused considerable inwashing which widened the capillary space. All joints broke with a brittle fracture; the shear strengths obtained are shown in Fig. 16. The plane of fracture was often within the original contour of the titanium specimen because of inwashing and fracture on the titanium-braze metal interface. The cause of brittle fracture is attributed to the copper content of the alloy.

The silver-manganese alloy, Alloy D, did not readily flow into capillary joint spaces. This poor flow is undoubtedly related to the manganese content but the metallurgical explanation was not established. The fracture of brazed joints indicated a slight tendency toward a ductile shear failure; however, the ductility was small compared with that in joints brazed with fine silver, and the braz-

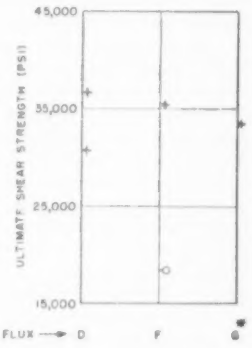


Fig. 15 Ultimate shear strengths of 1/8-in. single-lap titanium joints brazed with Alloy B using an oxy-acetylene torch

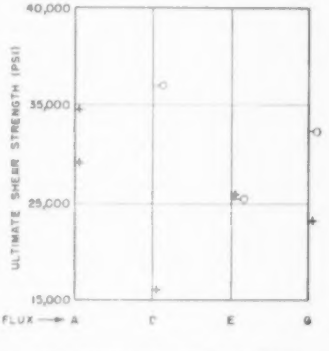
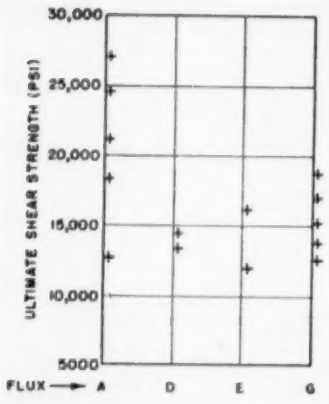


Fig. 16 Ultimate shear strengths of 1/8-in. single-lap titanium joints brazed with Alloy C using an oxy-acetylene torch



+ JOINTS HEATED WITH SINGLE TIP TORCH

Fig. 17 Ultimate shear strengths of 1/8-in. single-lap titanium joints brazed with 2S aluminum using an oxy-acetylene torch

ing alloy flow was so poor that further work with this alloy was terminated.

Molten 2S aluminum wets titanium and flows into the capillary space. Liquid aluminum quickly alloyed with titanium, and the resultant phases which form were retained by the relatively rapid quench that follows brazing. Figure 17 presents the ultimate shear strength data for brazed joints made with this alloy. The lower strength joints broke with a reasonable degree of ductility. Inasmuch as their strength was somewhat higher than the shear strength of annealed 2S aluminum (9500 psi), it is evident that the aluminum acquired some titanium in solid solution to form a higher shear strength metal. On the other hand those joints which had a higher strength broke with a brittle fracture. These variations in strength and ductility are related to differences in microstructure which forms because of the time-temperature variations in the brazing cycle.

Rather than braze with pure copper, the lower melting point nickel brass containing 67% Cu was studied. This avoids the high degree of melting and inwashing which occurs when pure copper melts and alloys with titanium. All joints made with the nickel brass alloy broke with a brittle fracture at shear strengths of 10,000 to 19,000 psi. Even this alloy was so erosive, no flux was needed to make a joint; however, the use of fluxes accelerated the initial wetting and reduced the heating cycle time, and thereby reduced the general erosion. All fractures occurred at the braze metal-titanium interface.

Studies were made of brazing titanium to stainless steel and exploratory tests were made trying to braze titanium to iron and aluminum. The problems involved and the work required to find a satisfactory brazing alloy and flux combination were discouraging. In the case of stainless steel, no satisfactory brazing alloy and procedure were found. Pure silver could not be made to wet and flow on stainless steel. All joints that were made with the silver base alloys, were extremely brittle at the stainless steel-braze metal interface; often joints broke in handling. The solution of titanium in the brazing alloy and its reaction with stainless steel to form brittle films at the stainless steel-brazing alloy interface is believed to be the metallurgical cause of brittleness. Considerable effort will have to be expended, studying diffusion barrier layers, in order to overcome this detrimental effect.

With regard to brazing aluminum to titanium, the aluminum, the titanium brazing fluxes and the potential brazing alloys have melting points in such proximity that a difficult experimental problem is posed when braze joining titanium to aluminum is attempted.

FURNACE BRAZING

Furnace brazing in an inert gas atmosphere is a simple method of brazing as far as fluxing is concerned; however, it requires a longer time cycle and as a result, metal alloying proceeds to a much greater degree. With regard to titanium, this permits metal combinations to produce thick film layers with consequent loss in joint ductility and somewhat lesser joint strength.

The basic advantage to furnace brazing lies in its ability to braze structures while maintaining a protective atmosphere. However, the metallurgy and joint properties are, at best, equivalent to those already discussed.

Inert atmospheres provide an excellent means to study the nature of metal depositing fluxes, and the flow of brazing alloys on titanium surfaces. For example, fine silver readily flows over surfaces heated in an inert atmosphere and fluxed with AgCl, Cu_2Cl_2 or $MnCl_2$ salts; Alloy A flows under similar conditions on surfaces fluxed with AgCl and Cu_2Cl_2 but not with $MnCl_2$. Silver and tin flow on a tin surface produced from $SnCl_2{\cdot}2H_2O$ salts, dissolve the film, and then dewet the titanium. This effect is believed to be related to the water vapor present in the stannous chloride; water provides a source of oxidant for producing a film between the molten silver or tin and titanium.

Mixed chlorides also have advantages. When mixed with KCl, both $MnCl_2$ and AgCl permitted braze metal to spread morg quickly then when these salts were used alone.

RESISTANCE BRAZING

A limited study of resistance brazed titanium joints was made using a setup shown in Fig. 18. For 1/8-in. sheet, 1 in. wide, or 3/32-in. sheet, 1/2 in. wide, a 3/16-in. overlap was used. A 3/16-in. wide brazing alloy sheet was placed in the joint interface after preparing the surfaces to be brazed using sandblasting followed by a 10% hydrofluoric acid pickle.

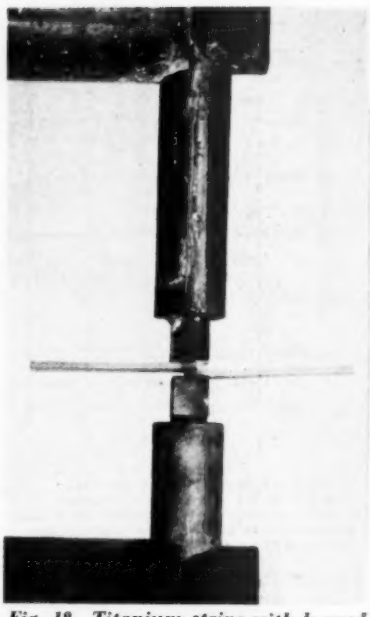


Fig. 18 Titanium strips with brazed metal sheet in the joint lap area placed between copper electrodes for resistance heating

Copper electrodes are recommended over graphite and other high electrical resistance electrode materials. Titanium has more than adequate electrical resistance to heat the faying surfaces; consequently, water-cooled high conductivity copper alloys are the best electrode materials.

Three factors must be controlled when making a resistance brazed joint, namely:

1. Electrode pressure.
2. Current density.
3. Heating time.

The selection of each was made on a trial-and-error basis. The principal difficulty encountered, using short brazing times, was nonuniform heating resulting from slight variations in total lap thickness and sheet geometry. In general, the joint center was heated to the melting point of the braze metal while the braze metal at the edges remained solid and bonded only by interdiffusion or recrystallization forces; if the brazing current was raised further to fuse the joint edges, the braze metal in the center was superheated, alloyed and inwashed with the titanium, and was squeezed out of the joint to form objectionably large fillets.

This effect was reflected in the fracture pattern of broken test specimens; it had an elongated elliptical spot in the center of the fracture, corresponding to the hot zone produced on brazing.

Using high currents and short times (5 to 8 cycles), a series of fine silver resistance brazed joints was made, Table 3.

Table 3—Ultimate Shear Strength of Resistance Heated Single 3/16-In. Lap Joints Brazed with Fine Silver

Specimen No.	*Brazing time, cycles**	*Approximate % of hot forging*	*Ultimate strength, psi*	*Description of joint*
1	5	None	10,600 U. S. S.	Shear fracture. Small quantity of brazed metal squeezed out of joint
2	6	None	19,900 U. S. S.	Shear fracture. Small quantity of brazed metal squeezed out of joint
3	7	25	46,800 U. T. S.	Titanium specimen fractured in tension at center of lap area. Considerable brazed metal squeezed out of joint
4	8	10	63,000 U. T. S.	Titanium specimen fractured in tension at center of lap area. Little brazed metal squeezed out of joint
5	7	None	9,600 U. S. S.	Ductile shear fracture at Ag-Ti interface. Incomplete fusion of silver. Heating current approximately 80% of amperage used in specimen No. 3
6	7	None	16,900 U. S. S.	Shear fracture. Little brazed metal squeezed out of joint. Heating current approximately 90% of amperage used in specimen No. 3

* 60∼/sec current.
Specimen size: 3/32- x 1/2-in. titanium strip. Thickness of fine silver sheet: 10 mils. Electrode pressure: 300 lb.

Such a procedure produced some joints having a high shear strength. With lower currents and longer times (6 sec to 4 min), joints having strengths shown in Table 4 were produced. The difference in joint strength is related to the degree of hot forging rather than to brazing time; longer brazing time encourages greater hot deformation, which is the real cause of the increased strength; joints made with long times, but little deformation, are relatively weak.

The point of least electrical resistance and greatest heat generation usually occurred at the center of the lap; on a second heating cycle, it occurred elsewhere. This suggested a repeated heating technique to assure greater probability of the entire joint receiving a more uniform maximum temperature throughout at some time during resistance brazing.

Joints were brazed with fine silver using a repeated heating technique. The conditions and results are described in Table 5.

It was possible to braze joints with pure copper by using the repeated heating technique. Copper foil, 1 mil thick, was used in order to minimize the quantity of Ti-Cu eutectic melt formed and the brazing temperature was kept to a minimum to reduce the tendency of the eutectic melt to erode titanium. The joints produced by this method are described in Table 6; the joint strengths were such that a number broke in the base metal rather than at the joint interface.

RECRYSTALLIZATION WELDING

When resistance brazing, considerable hot deformation may occur during which the molten brazing metal serving primarily as a cleaning and flushing agent is squeezed out of the joint; the clean faying surfaces are forced into intimate contact and bonded by recrystallization welding rather than by brazing. For example, the microstructure of high-strength joints which were resistance brazed using nickel brass foil are shown in Figs. 19 and 20. In Fig. 20, no evidence other than differential etching reveals the position of the original interface.

In Fig. 19, a definite interfacial phase is shown; nevertheless, high strength results because the joint thickness is very small, and the joint geometry forces flow and fracture in the base metal outside the lap area.

Figure 20 is a recrystallization weld even though it was made under conditions that would normally classify as brazing. In this specific case, recrystallization is effected by the allotropic transformation of titanium and the hot work which occurs on forging. To follow up this aspect of the problem, evaluation of recrystallization welding of commercially pure and a 7% Mn titanium alloy was made in a preliminary way following the procedures established in a previous study.[9]

Basically these consist of surface machining titanium and then pressing the machined surfaces between heated dies to make a lap-type specimen, which was subsequently tested in tension. Table 7 presents the results of this evaluation.

Welding of pure and 7% Mn titanium can be achieved using interface temperatures approximately 540 and 570° C

Table 4—Ultimate Shear Strength of Resistance Heated Single 3/16-In. Lap Joints Brazed with Fine Silver

Specimen No.	*Time of heating*	*Approximate % of hot forging*	*Ultimate shear strength, psi*	*Description of joint*
1	6 sec	None	20,300	Ductile shear fracture in silver. Silver did not fuse
2	1 min	None	23,750	Silver fused in lap interior. Silver in joint edges did not fuse
3	1 min, 12 sec	20	27,200	Small amount of brazed metal squeezed out of joint
4	2 min	None	17,800	Silver fused in center of lap area. No brazed metal squeezed out of joint. Heating current approximately 90% of amperage used in specimens 1, 2 and 3
5	2 min	None	16,430	Ductile shear fracture in silver. Silver did not fuse. Heating current approximately 80% of amperage used in specimens 1, 2 and 3
6	4 min	None	18,700	Same as specimen No. 5

Specimen size: 3/32- x 1/2-in. titanium strip. Thickness of fine silver sheet: 10 mils. Electrode pressure: 300 lb.

Table 5—Ultimate Shear Strength of Resistance Heated Single 3/16-In. Lap Joints Brazed with Fine Silver Using Repeated Heating Cycles

Number of heatings	*Ultimate strength, psi*	*Approximate % of hot forging*	*Description of joint*
1	11,800	None	Ductile shear fracture. Silver did not fuse. Fracture at Ag-Ti interface
2	17,200	None	
5	17,200	None	
11	16,300	None	

Specimen size: 1/4- x 1-in. titanium strips. Thickness of fine silver: 10 mils. Electrode pressure: 700 lb. Time of heating: 24 sec.

Table 6—Ultimate Strengths of Resistance Heated Single $^{3}/_{16}$-In. Lap Joints Brazed with Pure Copper Using Repeated Heating Cycles

Number of heatings	*Electrode pressure*	*Approximate % of hot forging*	*Ultimate strength, psi*	*Type of fracture*
3	500	None	27,700	1*
7	500	None	49,600	2†
11	500	10	53,200	2†
3	700	None	12,200	1*
7	700	5	47,600	2†
11	700	10	59,400	2†

* Joint fractured in shear—reported strength values are ultimate shear strength.
† Joint fractured in tension on edge of lap area—reported strength values are ultimate tensile strength.
Specimen size: $^{1}/_{2}$- x 1-in. strips. Thickness of copper sheet: 1 mil. Time of heating: 30 sec.

Table 7—Relationship Between Welding Temperature and Weld Shear Strength for Recrystallization Welds Made in $^{1}/_{16}$-In. Sheet of Commercially Pure Titanium and 7% Mn Titanium Alloys Welded with $^{1}/_{8}$-In. Heated Dies

Metal	*Interface temperature, ° C*	*Shear load, lb*	*Weld diam, in.*	*Shear strength, psi*	*Comments*
75A (2-min bonding time)	482	...	...	...	No bonding
	524	250	0.0825	46,800	
	538	1040	0.150	59,000	
	538	972	0.140	63,100	
	560	550	0.145	33,400	
	574	434	0.130	32,600	
	574	1174	0.145	71,200	
130A (2-min bonding time)	524	...	...	...	No bonding
	538	...	...	...	No bonding
	549	620	0.08	123,000	
	574	1023	0.10	131,000	
	579	1198	0.11	126,000	

or higher, respectively. The strength of the welded interface is that of the shear strength of the annealed metal. Using surface cold work as the mechanism of creating recrystallization bonding, it is not necessary to go to the allotropic transformation temperature to create recrystallization; hence, temperatures of the order of 550° C may be used. This may prove of value in joining titanium to make high-strength joints without producing embrittlement which may result because of resolution of alpha phase at the higher temperatures and the aging tendencies which subsequently occur.

Figure 21 shows a typical structure of such a recrystallization weld in 7% Mn, bal. Ti alloy. It will be noted in this structure that there is no tendency for rejected α-phase to reform β, hence, the brittle transition structures that form on joining at higher temperatures cannot occur; as a result, joint embrittlement is not a problem.

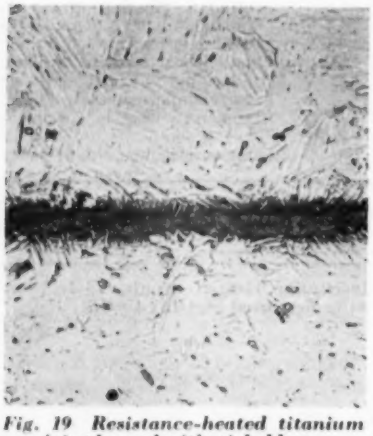


Fig. 19 Resistance-heated titanium joint brazed with nickel brass

The joint was virtually a recrystallization weld in which the titanium was bonded with a thin metallic phase film.
Etchant: 20% HF + 20% HNO_3 + 60% glycerin. Magnification × 500.

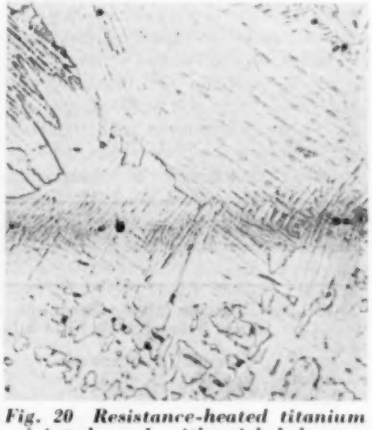


Fig. 20 Resistance-heated titanium joint brazed with nickel brass

The joint was a recrystallization weld. No brazed metal was found in the joint. The recrystallized grain structure in the former joint interface is in common with both sections of titanium base metal.
Etchant: 20% HF + 20% HNO_3 + 60% glycerin. Magnification × 500.

SUMMARY

1. Ductile lap brazed joints were made using the oxyacetylene torch, fine silver, and certain electrochemically depositing fluxes. These joints have an average unit shear strength of the order of 31,500 psi with a standard deviation of 5400 psi.

2. Copper base alloys and copper bearing brazing alloys cause brittle joints when brazing titanium.

3. Some titanium joints brazed with 2S aluminum were ductile; however, all high-strength aluminum brazed joints broke with a brittle fracture.

4. Metal depositing fluxes were developed for brazing titanium, and proved to have considerable advantage over standard commercial fluxes, particularly when using fine silver as a brazing alloy.

5. Resistance brazed joints can be made without flux; properly made joints show high strengths and force failure to occur outside the lapped joint.

6. Recrystallization welding of titanium was shown to be possible; strengths of such joints were equal to those of the annealed metal.

7. Furnace brazed lap joints may be made in impure inert gas atmospheres using fluxes containing metal depositing fluxes containing AgCl and Cu_2Cl_2.

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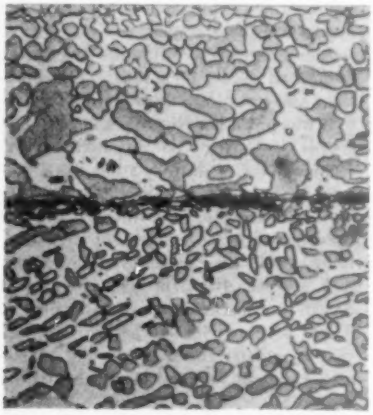

Fig. 21 Joint of recrystallization welded 7% Mn-titanium base alloy

The dark etched line is the plane of the joint interfaces.
Etchant: 20% HF + 20% HNO_3 + 60% glycerin. Magnification × 1000.

Table 8—Melting Points of Titanium Brazing Flux Mixtures

Flux mixture	*Initial melting temperature, ° C*	*Complete fusion temperature, ° C*	*Remarks*
A	607	622	Considerable boiling immediately after complete fusion. Solidified flux after fusion is white
	595	634	
E	513	562	Slight boiling immediately after complete fusion. Solidified flux after fusion is white
	504	543	
D	526	507	Color commenced to darken after initial fusion. Flux boiled moderately after complete fusion. Solidified flux after fusion was white with interspersed black specks and covered with a dark gray surface film
	513	558	
G	507	554	No boiling. Color darkened after initial fusion and gradually increased in darkness to a black color at complete fusion. Solidified flux after fusion is a gray-black color
	513	546	

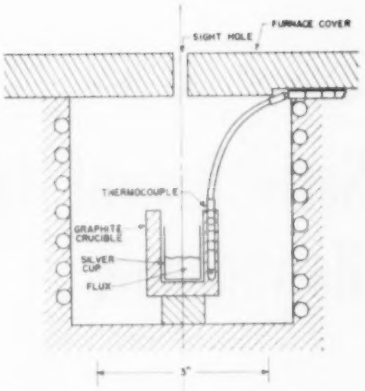

Fig. 22 Arrangement of equipment in a resistance-heated furnace for measuring the melting point of fluxes

ices, who discovered the silver-titanium compound and established its existence; of J. C. Taylor, of the Physics Department, who made the electron diffraction studies of titanium surfaces; and of R. Van Thyne, H. Kessler, H. Mayer, W. Rostoker and others who determined portions of the silver-titanium phase diagram and the crystallographic structure of the silver-titanium compound.

Appendix

MELTING POINT DETERMINATIONS OF FLUX MIXTURES

A limited evaluation of the melting temperatures of titanium brazing fluxes was made using the arrangement shown in Fig. 22. Table 8 gives the results of this limited survey. Two melting determinations were made for each flux mixture.

A graphite crucible provided mechanical support for a thin silver cup, into which various flux mixtures are placed. When making a melting point determination, the furnace was heated to about 200° C below the fusion temperature of the flux, the flux then placed in the silver cup and the furnace temperature slowly heated at approximately 2° C per minute.

The approximate temperature of incipient fusion was determined by means of a thermocouple at the time when glazing was first observed through the sight hole in the top of the furnace. Because fluxes are usually compounded by mixing salts, it is recognized that a second melting determination will yield a lower fusion temperature; this value, however, was not determined because it has little significance with regard to brazing.

Flux A boils considerably immediately after complete fusion and the solidified flux is white. The same effect is observed for flux E; however, the boiling is only slight. Flux D begins to darken after initial fusion and it boils moderately after complete fusion; the solidified flux has black specks interdispersed in a white matrix and is covered with a dark gray film. Flux G, a commercial flux, shows no boiling on fusion; it darkens after initial fusion and gradually becomes black at complete fusion. Solidified flux G is a gray-black color.

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Some Recent Advances in the Welding of Molybdenum

To secure ductile welds, it is necessary that the faying surfaces of specially purified molybdenum be cleaned carefully and the welding be done in high vacuum

by W. H. Kearns, H. B. Goodwin, E. Eichen and D. C. Martin

Abstract

Commercial molybdenum can be readily welded by conventional resistance welding and inert-gas-shielded, tungsten-electrode arc welding. However, the welds are brittle at room temperature. By using extremely pure molybdenum, ductile welds have been obtained, using resistance upset welding. To secure ductile welds, it is necessary that the faying surfaces be cleaned very carefully and that the welding be done in high vacuum. Electropolishing was the preferred cleaning method. Two methods were developed for preparing molybdenum of sufficient purity that ductile welds could be made in it. One of these was multiple vacuum-arc melting; the other, heating above 3500° F for long times in high vacuum.

INTRODUCTION

THE high-temperature strength and relative abundance of molybdenum suggest that molybdenum-base alloys would be useful in the construction of heat engines for operation at higher temperatures than are presently possible. The best alloys now available do not retain useful strength above about 1650° F; moreover, the cobalt and nickel which are major constituents of these alloys are critically scarce. Molybdenum alloys have been made which possess useful strength at 2000° F, and better ones can no doubt be developed. The only metals with higher melting points than molybdenum are tungsten, rhenium, tantalum and osmium, and these are all in short supply or come from foreign sources.

Unfortunately, the potential usefulness of molybdenum is, as yet, unrealized, primarily because of two major difficulties. One of these, lack of high-temperature oxidation resistance, is outside the scope of this paper. The other difficulty is that welds in present-day commercial molybdenum are almost always brittle at room temperature, so that fabrication of desired structures is often difficult or impossible.

W. H. Kearns, H. B. Goodwin, and **D. C. Martin** are connected with the Battelle Memorial Institute, Columbus 1, Ohio. **E. Eichen**, formerly with Battelle, is at the Ohio State University.

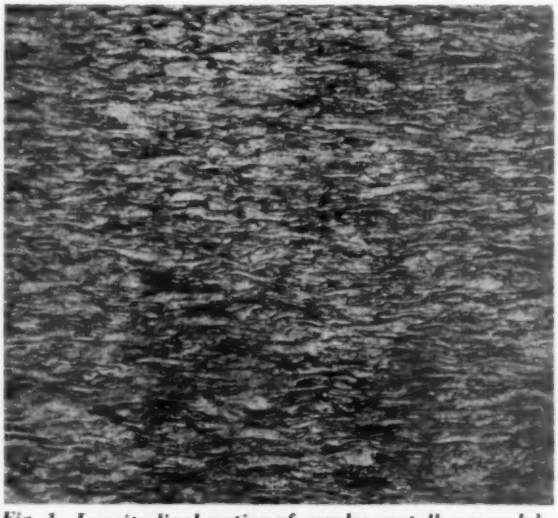

Fig. 1 Longitudinal section of powder-metallurgy molybdenum rod showing typical fibered structure. × 100

Both powder-metallurgy and arc-cast molybdenum, the two types commercially available, have some ductility when they have a "fibered" microstructure as a result of cold work.* A typical fibered microstructure is shown in Fig. 1. Even this limited ductility disappears after welding (or any other heat treatment which causes recrystallization). In welds, the metal in both the fused zone and the recrystallized zone is brittle. With powder-metallurgy molybdenum, welds tend to be porous as well as brittle.

So far as welding technique is concerned, there is no particular difficulty in welding molybdenum by inert-gas-shielded, tungsten-electrode arc welding, and by resistance welding, provided precautions are taken to exclude air from contact with the hot metal. Very smooth, neat-appearing arc welds can be obtained in both arc-cast and powder-metallurgy molybdenum. Porosity is usually not encountered in arc welds made in arc-cast metal. Molybdenum is commercially welded

* Here cold work means working at any temperature below the recrystallization temperature.

in the electronic-tube industry. Such welds are satisfactory for the intended use. The parts are small and brittleness is not a serious problem. For larger structures, however, most welds have been unsatisfactory.

A research program to study the general problem of room-temperature brittleness in molybdenum has been in progress at Battelle Memorial Institute for about three years under sponsorship of the Office of Naval Research. In addition, the Atomic Energy Commission and the Office of Naval Research have jointly supported research on the welding of molybdenum. Grateful acknowledgment is made to both of these agencies for their support of the work presented in this paper.

It had been established, prior to the present investigation, that the room-temperature ductility of molybdenum can be destroyed by small amounts of oxygen and that arc-cast molybdenum was not (hot) forgeable if it contained over 0.0025% oxygen. Deoxidation by adding carbon or aluminum is necessary to obtain forgeable arc-cast molybdenum. A residual carbon content of about 0.03% appears necessary to insure forgeability if carbon is the deoxidizer used. Powder-metallurgy molybdenum can tolerate somewhat more oxygen than arc-cast molybdenum and still be forgeable. Arc-cast molybdenum is typically brittle in the cast state, although individual grains exhibit some ductility.[1]

Typical analyses for carbon-deoxidized, arc-cast molybdenum, and for powder-metallurgy molybdenum are given in Table 1.

EARLY ATTEMPTS AT NONFUSION WELDING

Because brittleness in molybdenum had always been associated with a recrystallized structure, the first approach to the problem of making ductile molybdenum weldments was to attempt to weld in the solid state, without fusion, and to keep the metal below a time-temperature boundary chosen to avoid recrystallization. Welds would thus be formed by diffusion across the interface. Initially, attempts were made to spot weld sheet molybdenum, but experimental difficulties prompted a change to resistance upset welding using $^1/_4$-in.-diam rod in a flash-welding machine. The advantages of resistance upset welding are discussed in detail later. Ductility was evaluated by means of bend tests, to be described later. Welding temperatures ranging from 2500 to 4000° F and times of heating up to several minutes were tried. Although some welds were obtained, most of them were weak and none were ductile.

CLUES LEADING TOWARD DUCTILE WELDS

The first important clues leading toward significant improvement in the strength and ductility of molybdenum weldments were obtained from other research on the ductility of molybdenum not directly aimed at the welding problem. In this research, done at Battelle, two important findings were made. These were that:

1. Cast molybdenum is ductile if it is sufficiently pure.
2. In some cases, certain heat treatments will restore limited ductility to powder-metallurgy molybdenum which has been embrittled by recrystallization.

The effect of purification on the ductility of cast molybdenum has been reported upon in detail by Reng-

Table 1—Typical Chemical Content of Commercial Powder-Metallurgy and Arc-Cast Molybdenum

Type of Molybdenum	Chemical Content, per cent															
	O_2	H_2	N_2	C	S	Fe	Si	Al	Mn	Cr	Cu	Pb	Bi	Mg	Ti	Ni
As Received Powder Metallurgy	0.0019 -0.015	0.00004	0.0008	0.003	0.004	0.020	0.002	<0.001	<0.001	<0.001	0.0002	<0.001	0.0001	<0.0001	<0.005	0.001
Commercial Arc Cast	0.0006	0.0009	0.0005	0.044	0.017	0.002	0.001	<0.001	<0.001	<0.001	0.0003	<0.001	<0.0001	<0.001	<0.005	<0.001
High Purity Cast	0.0002	0.00005	0.0002	0.002	0.001 to 0.003	0.001	0.0005	<0.001	<0.001	<0.001	0.0003	<0.001	<0.0001	<0.001	<0.005	<0.001
Solid-State Purified 3500 F - 6 hours	0.0003	0.00002	0.0002	0.003	0.004	Same as the as-received molybdenum										
Solid-State Purified 3500 F - 12 hours	0.0003	0.00002	0.0003	0.004	0.005	Same as the as-received molybdenum										
Solid-State Purified 3500 F - 24 hours	0.0005	0.00002	0.0002	0.012	0.004	Same as the as-received molybdenum										
Solid-State Purified 3800 F - 12 hours	0.0003	0.00002	0.0002	0.005	0.004	Same as the as-received molybdenum										

storff and Fischer.[2] Special vacuum arc-melting techniques were developed for preparing molybdenum of "superpurity." A typical analysis of this material is given in Table 1. Bend tests (of a type to be described later) showed this material to be more ductile in cast form than is commercial molybdenum with a fibered structure. The implication of this finding was that molybdenum which was ductile in the as-cast state might also be ductile after fusion and/or recrystallization during welding.

With regard to heat treatment, it was observed that powder-metallurgy molybdenum wires (originally of fibered structure and, hence, having some ductility) were completely brittle when recrystallized at 3600° F or higher. By reheating them to between 2500 and 3000° F, some ductility could be restored. This suggested that ductility might be restored to welds by a similar treatment.

These findings guided all subsequent welding research.

WELDING METHOD

The resistance upset-welding process, using round molybdenum rod specimens, was selected as the welding method to be used in the research for the following reasons:

1. Close control of the welding variables can be maintained.
2. The welded specimens require little or no machining prior to testing and are suitable for the bend test used to evaluate ductility. (Reasons for using the bend test are discussed later.)
3. No filler metal is required.
4. With this type of specimen, it is easier to devise means of welding in vacuum or in a controlled atmosphere for protection against oxidation.
5. Postweld heat treatment, if desired, is readily accomplished by self-resistance heating.

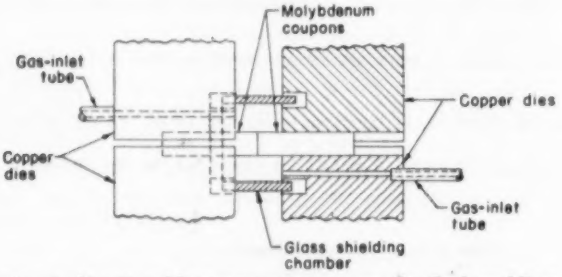

Fig. 2 Upset welding arrangement on the flash-welding machine

Initially, welds were made in flowing argon, helium or hydrogen atmospheres to protect the specimens from oxidation. To contain the protective atmospheres, a special chamber was installed on a 30-kva, motor-driven, flash-welding machine as shown in Fig. 2. Later welds were made in vacuum when it was found that the atmospheres did not give sufficient protection. For the first vacuum welding tests, a vacuum arc furnace ordinarily used for arc melting molybdenum was modified to permit resistance upset welds to be made in it. In later experiments, a vacuum chamber was fitted to a standard spot-welding machine. This is shown in Fig. 3.

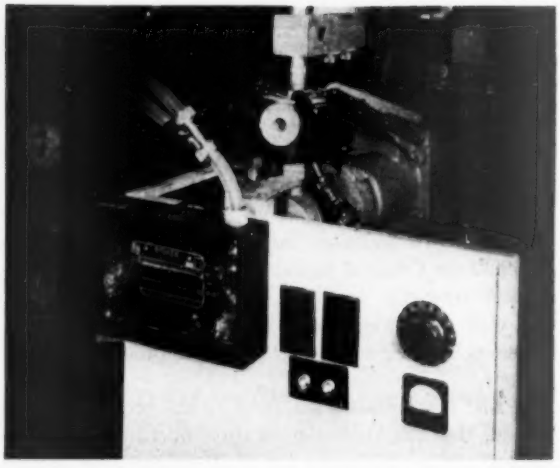

Fig. 3 Vacuum upset-welding chamber on spot-welding machine

METHOD OF TESTING THE WELDED JOINTS

Because of the small size of the welded specimens, it was impractical to use a tensile test for testing the ductility of the weldments. Initially, welded specimens were tested in constant-moment bending. With this test, a stress can be applied uniformly over the fusion and heat-affected zones at a very low strain rate. The arrangement for the constant-moment bend test is shown in Fig. 4*a*. After ductile upset welds were obtained, a simply supported, centrally loaded bend test

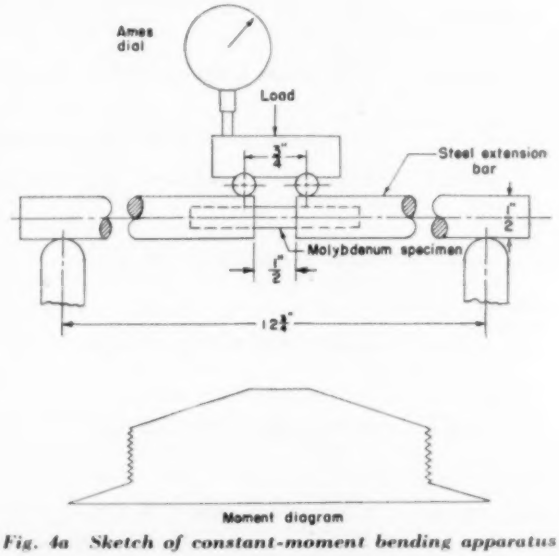

Fig. 4a Sketch of constant-moment bending apparatus

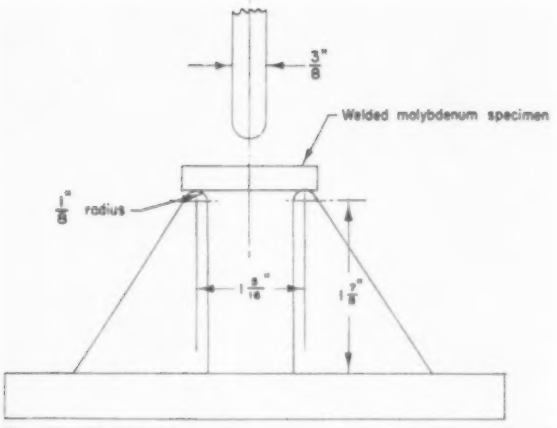

Fig. 4b Sketch of simply supported centrally loaded bend jig

was used as shown in Fig. 4*b*. This arrangement permitted testing at a higher strain rate, but the center of the specimen (fusion zone) is strained more than other regions. The unwelded material was also tested for ductility by means of a similar bend test. In a few tests, a shorter span than that indicated in the figure was employed because of lack of material. In this text, it should be understood that the tests were made on specimens $1^5/_{16}$ in. in length, unless it is specifically stated otherwise.

The criterion of ductility originally was taken simply as the angle through which the specimen could be bent before it fractured, when tested at room temperature (70–80° F) and at a very low strain rate. Only plastic deformation (permanent bend angle), not elastic, was measured. From the bend angle, per cent elongation in the outermost fibers of the specimen could be calculated.

Later, as more ductile molybdenum and molybdenum welds were produced, it was apparent that a more discriminating criterion was needed to show small differences in ductility. A convenient criterion proved to be the lowest temperature at which any plastic deformation was observed prior to fracture in a series of single-point-loaded bend specimens. The lower the temperature at which some plastic deformation could be obtained, the more ductile the material could be considered. This temperature may be thought of as a ductile-to-brittle "transition temperature" and is somewhat analogous to the transition temperature determined from impact testing of steels.

For determining the "transation temperature," a series of similar specimens was bent at the desired deflection rate. The temperature of testing was progressively lowered from specimen to specimen. For each series of similar specimens, a plot was made of permanent bend angle just prior to fracture against temperature. Although there was considerable scatter in individual data points, an average smooth curve was drawn through them. The transition temperature was arbitrarily taken as the intersection of the curve with the zero-degree bend line. Bend angles can be mathematically converted to strain in the outermost fibers, and deflection rates converted to strain rate in the outermost fibers.

WELDING HIGH-PURITY MOLYBDENUM

Molybdenum which had exhibited ductility in the cast condition had been prepared by arc melting commercial powder-metallurgy molybdenum three times in high vacuum (pressures of less than 0.1 micron), using water-cooled copper crucibles and semi-inert (water-cooled) molybdenum electrodes. Melting stock was added primarily in crushed form (between one-quarter and one-third of the ingot comes from the electrode melting off). This melting method permits a slow melting rate and, hence, considerable time for purification.

Purification apparently results simply from the fact that practically all the impurities have a higher vapor pressure than molybdenum at its melting point. Little carbon is removed. Therefore, oxygen removal evidently results from volatilization of the oxide rather than from formation of CO. Analyses showed that both metallic and gaseous impurities were removed by multiple vacuum meltings. Analytical data for this high-purity molybdenum are given in Table 1.

Molybdenum is usually fabricated into structures in the wrought condition. Therefore, the first welding tests on triple vacuum-melted molybdenum were made with the metal in the wrought condition. High-purity ingots were hot forged into bars from which test coupons, $^1/_4$ in. in diameter and 1 in. long, were machined. This size coupon was standard throughout the work.

The forged high-purity molybdenum was first upset welded in hydrogen on the flash-welding machine, using the gas shield previously described. The hydrogen was dried by passing it through two freezeout tubes at dry ice temperature (−110° F). The faying surfaces of the coupons were polished on emery and crocus cloths and washed with carbon tetrachloride.

The welded joints were tested for ductility in constant-moment bending at room temperature. All of the joints were brittle at a strain rate of 10^{-5} in. per inch per second in the outermost fibers of the specimens.

Fractures were intergranular and, when examined under a microscope, were seen to have impurities on the grain surfaces. It was evident that the material was being contaminated, but it could not be determined whether it was being contaminated during forging or during welding. Therefore, the forging operation was eliminated by welding the high-purity molybdenum in the as-cast state.

Test coupons were machined directly from high-purity ingots. Attempts to upset weld two such coupons together were made in dried hydrogen and in helium, but all welds were brittle when tested in the same manner as the forged high-purity molybdenum.

The fractures were intergranular. When the fractures were examined microscopically, impurities were seen on the grain surfaces. This indicated that the molybdenum was being contaminated during welding, possibly by impurities in the shielding atmosphere. Consequently, welding in high vacuum was resorted to.

The first experiments with welding in high vacuum (about 0.1 micron) were made in the vacuum arc furnace modified for welding.

Various methods of surface preparation, prior to welding, were tried. After several attempts, a weld which had considerable room-temperature ductility was produced. Surface preparation consisted of polishing the faying surfaces successively on Nos. 240, 400 and 600 metallographic paper, polishing with rouge on a cloth wheel and finally electropolishing in Coon's solution.* The welded specimen bent 33 deg at room temperature before fracture in constant-moment bending. This was an elongation of about 14% in $^1/_2$ in. in the outermost fibers. In these fibers, the strain rate was about 10^{-5} in. per inch per second. This result was verified by producing several additional ductile welds in the same manner.

Attempts were made to weld the high-purity material under less rigorous (and more easily attainable) conditions. All attempts to weld in gas atmospheres produced brittle welds in spite of most elaborate precautions to purify the gases. A weld was made after cleaning the faying surfaces with a new steel file, which had been carefully washed in ether to remove all grease. This weld exhibited some ductility in the bend test in spite of a large void in the weld zone, suggesting that mechanical preparation of the interfaces might be satisfactory, provided care was taken to insure extreme cleanliness. However, for further experimental work, the electropolishing method seemed most reliable.

The preparation of high-purity molybdenum by multiple vacuum arc melting was time consuming and expensive. It was reasoned that at least the gaseous impurities present in commercial molybdenum might diffuse to the surface and volatilize if the metal were heated in vacuum to a high temperature, somewhat below the melting point. If the operation were carried on long enough, the same purification might be obtained as from vacuum melting. The results of other research† prompted this line of reasoning. A number of molybdenum wires, heated in vacuum above 4000° F for another purpose, had turned out to be ductile, although completely recrystallized.

Pieces of $^1/_4$-in. commercial powder-metallurgy rod from a single lot of molybdenum were resistance heated at temperatures of 3500 and 3800° F for long periods of time in a high vacuum (less than 0.1 micron). For convenience, this type of heat treatment is usually referred to in our laboratory as "solid-state purification" treatment. Over a period of time a great many experiments with solid-state purification have been made.

* 150 ml methyl alcohol, 50 ml hydrochloric acid, 20 ml sulfuric acid.

† Unpublished Battelle Research.

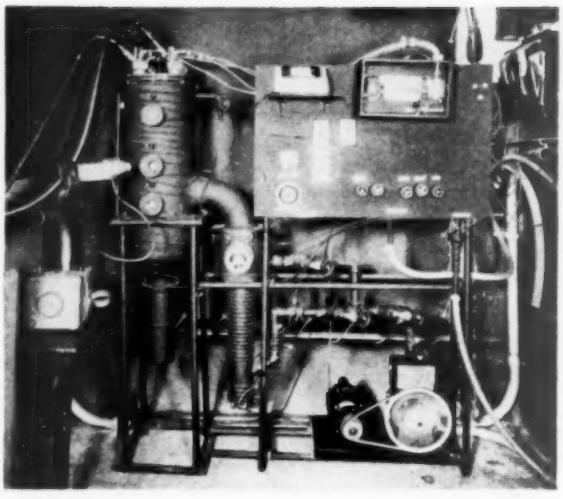

Fig. 5 Vacuum heat-treating furnace

Occassionally, for an as yet unknown reason, some specimens are brittle after a treatment which normally produces ductile molybdenum. Such specimens have not been used in welding experiments. The vacuum heat-treating furnace is shown in Fig. 5. It is similar to the furnace described by Few and Manning,[3] but somewhat larger. The conditions used for heat treating and the analytical results are shown in Table 1. Oxygen and nitrogen were progressively removed, but carbon and hydrogen increased during treatment. Metallic-impurity content did not change. This is in contrast to the vacuum-melting method which removes metallic as well as gaseous impurities.

The ductility of the solid-state-purified rods was measured by determining their transition temperatures with a bend test. Since the amount of material was small, the single-point-loaded bend test was modified to use a $^5/_8$-in. span and a $^1/_{16}$-in-radius load point. The transition temperatures of the solid-state-purified samples are shown in Table 2, which also gives the transition temperatures of as-received rod and vacuum-fusion purified molybdenum. The vacuum heat treatment had definitely improved the ductility of the molybdenum.

Table 2—Transition Temperatures of Solid-State-Purified Molybdenum Samples

Vacuum heat treatment		*Transition temperature, ° F,* at a strain rate of 0.06¼ per sec*
Temperature, ° F	*Time, hr*	
3500	6	68
3500	12	−50
3500	24	−50
3800	12	−90
As-received powder metallurgy		5
Vacuum-fusion purified†		−70

* The transition temperatures were determined in a single-point-loaded bend test. They were the temperatures at which no permanent set was observed in the broken test specimens.

† The specimens used in these tests were rectangular, and the transition temperature at this strain rate was estimated from other data.

The chemical compositions of the samples heated for 6 and 12 hr were about the same, yet the transition temperature of the 12-hr sample was much lower than that of the 6-hr sample. Metallographic and fractographic examination showed that the 12- and 24-hr samples had a second phase at the grain boundaries, while the 6-hr sample did not. In view of the fact that the 24-hr sample had picked up carbon during vacuum heat treatment (Table 1), it appeared that the second phase was carbide. Its appearance was associated with low transition temperatures, provided there was only a very small amount present at the grain boundaries. The only known source of carbon in the furnace is hydrocarbon oil vapors that back diffuse from the diffusion pump. It may be that deoxidation must be almost complete before carbon pickup can start.

When upset welds were made in solid-state purified ductile molybdenum, the welds themselves were also ductile, provided they were made under the same carefully controlled conditions of high vacuum and special surface preparation used for the high-purity vacuum-arc-melted molybdenum. They bent from 45 to 90 deg at room temperature at a strain rate of 10^{-4} in. per inch per second using the single-point-loaded bend test. In the most ductile specimens, the elongation in the outer fibers of the welds was about 40%.

The important finding was not that solid-state purified rods were usually ductile after the solid-state-purification treatment, but that those which were ductile could be welded and still retain ductility in the welded joint.

From these results, there was every reason to believe that the welds would be ductile at much higher strain rates at room temperature. When bend tested in the same manner and at the strain rate (0.064 in. per inch per second) used to test unwelded material, the transition temperature of the welds was −75° F which was of the same order as the transition temperature of material from which the welding coupons were taken.

A macrograph of a polished and etched section of a ductile weld in solid-state-purified molybdenum is shown in Fig. 6. In high-purity molybdenum, porosity in the welds was almost completely eliminated. It was concluded that the porosity usually present in welds in untreated commercial powder-metallurgy molybdenum is caused by impurity elements which are removed during solid-state purification.

Since welds in untreated commercial molybdenum are brittle and welds in solid-state-purified molybdenum are ductile, and since the only elements known to be removed by solid-state purification are oxygen and nitrogen, it may be tentatively concluded that one or both of these elements is the usual cause of brittleness in molybdenum weldments.

Tests were made to determine what level of atmospheric pressure contaminated upset welds in high-purity molybdenum. Several upset welds were made *in vacuo* at (air) pressures ranging from 0.5 to 2 microns. When the welded joints were tested in the single-point-loaded bend jig with a $^5/_8$-in. span at room temperature at a strain rate of 0.064 in. per inch per second, it was found that welds made at pressures of 1 micron or above were brittle and that less than 0.5 micron was necessary for maximum ductility. This suggested that inert atmospheres containing more than about 1 part per million of impurities would embrittle welds in high-purity molybdenum. Inert gases of this purity are nearly impossible to prepare.

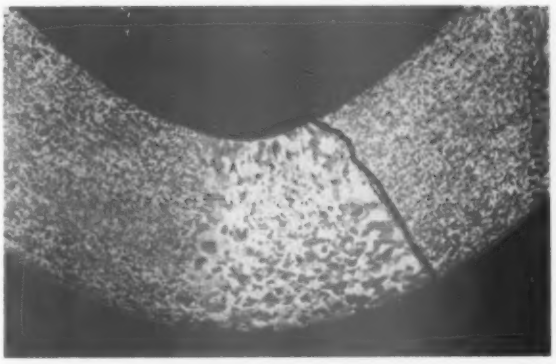

Fig. 6 Ductile upset weld in solid-state-purified molybdenum. × 5

WELDING COMMERCIAL MOLYBDENUM

Concurrently with the experiments on welding high-purity molybdenum, the welding of commercial molybdenum followed by various postweld heat treatments was investigated. Powder-metallurgy and arc-cast molybdenum were used in the form of $^1/_4$-in.-diam rod. The powder-metallurgy rod had been swaged and the arc-cast rod had been forged and rolled. They had long, fibrous grain structures and, as received, were ductile at room temperature. For example, the powder-metallurgy molybdenum rod bent 23 deg prior to fracture at a strain rate of 0.064 in. per inch per second in the outermost fibers, and the arc-cast molybdenum bent 65 deg at 0.038 in. per inch per second. Both types of molybdenum were tested in the single-point-loaded bend test with a $^5/_8$-in. span.

A number of upset welds were made in dried hydrogen and helium using both types of molybdenum. As-welded joints in the powder-metallurgy molybdenum were extremely brittle in the constant-moment bend test described previously* at the low-strain rate of 10^{-5} in. per inch per second. The welds were very porous. Welds made in vacuum with commercial powder metallurgy molybdenum were also brittle and porous.

Welds made in the wrought arc-cast molybdenum bent through angles of 5 to 8 deg at the same strain rate. The elongations were estimated at 2 to 3% in $^1/_2$ in. This was not felt to be sufficient for engineering usefulness. Upset welds made with arc-cast molybdenum in high vacuum were brittle when bend tested at a similar strain rate.

* This test was used to evaluate the ductility in all the tests made on commercial molybdenum.

As previously stated, other research had shown that powder-metallurgy molybdenum wires were brittle after being recrystallized at 3600° F. However, some ductility was restored by reheating them to between 2500 and 3000° F.* This suggested that welds in powder-metallurgy molybdenum might be made ductile by using a postweld heat treatment, although it was realized that porosity would still be a problem.

Further tests made to reconfirm the effect of heat treatment showed that unwelded powder-metallurgy molybdenum rod was brittle at a strain rate of 10^{-6} in. per inch per second after it had been heated at 3800° F for 20 sec in flowing dried hydrogen. After reheating to 2750° F for 1 to 2 min and quenching, the specimens bent through angles of 23 to 30 deg at the same low-strain rate at room temperature. The elongations were about 10 to 13% in $^1/_2$ in. Similarly heat-treated unwelded arc-cast specimens bent about 30 deg.

With this encouragement, upset welds were made (in hydrogen) in powder-metallurgy molybdenum rod and were postweld heat treated at 2750° F for 2 min. They bent through angles of 3 to 6 deg when bent tested in the same manner as the unwelded, heat-treated rods. The elongations were about 1 to 2% in $^1/_2$ in. Thus, the welded specimens were not so ductile as the unwelded, heat-treated rods. Apparently, the heat treatment cannot overcome the harmful effect of porosity, which was present in the welded samples but not present in those simply heated so as to simulate the heat cycle of welding.

Upset welds in wrought, arc-cast molybdenum were similarly postwelded heat treated in dried hydrogen. The heat treatment did not improve the ductility of the welds (as previously stated, welds in this material had slight ductility without heat treatment). The fractures were mostly transgranular, but they originated at a point of intergranular fracture. In the regions where the fractures were intergranular, the grains were covered by a continuous network of carbide. Metallographic examination showed that all the grains in the weld were surrounded by carbide. It was concluded that the high carbon content (0.030% or more) of the molybdenum was responsible for the brittleness in the welds.

SUMMARY

This research has shown that it is possible to make sound ductile welds in molybdenum. The molybdenum must be of very high purity. Molybdenum of sufficient purity can be prepared either by multiple vacuum arc melting or by high-temperature heat treatment of commercial powder-metallurgy molybdenum in high vacuum (0.1 micron). The latter treatment occasionally does not produce ductile molybdenum for an as yet unknown reason. The welding must be done in high vacuum to avoid contamination of the weld. Commercially available inert gases are too impure to provide adequate protection during welding. Extreme cleanliness of the faying surfaces is necessary if the weldments are to be ductile.

It does not appear feasible to obtain really ductile fusion welds in presently available commercial molybdenum. In the case of powder-metallurgy molybdenum, the oxygen content (and possibly the nitrogen content) is too high, resulting in porosity and brittleness. The brittleness of welds in arc-cast molybdenum is probably the result of its high carbon content. The carbon is added during melting to remove oxygen.

* In this investigation, a very large number of heat treatments were tried, and this was the only one which improved ductility very much.

ACKNOWLEDGMENT

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Welding for Low-Temperature Service

An evaluation of the properties of weld metal for fabricating heat exchangers and vessels for low-temperature service

by Robert W. Bennett

Abstract

This paper deals with the properties of the various weld metals and base materials used in the fabrication of pressure vessels and heat exchangers for low-temperature service and with certain tests used for their evaluation. Transition temperature curves of the Charpy notched-bar test data of various types of weld metals have indicated that AWS Class E-6020 and E-7020 electrodes will meet ASME Code requirements for welding killed carbon steels for service to −50° F.

For service to −150° F, certain brands of AWS Class E-8016 electrodes have been suitable for welding low-alloy carbon steels such as 3½% nickel and the chromium-copper-nickel steels. Correlation of the metallurgical structure of weld metal with Charpy notched toughness has indicated that a welding technique producing fine-grain weld metal is essential for optimum notch toughness.

INTRODUCTION

METALLURGISTS and engineers have long been aware that certain steels are more likely to fail in service at subzero temperatures than at ambient or slightly elevated temperatures. Considerable effort has been spent during the past ten years in trying to evaluate the usefulness of various mechanical tests of small welded-steel specimens for indicating the performance of large welded structures.

The pressure-vessel industry, however, still uses the standard keyhole Charpy notched-bar specimen prepared and tested under the conditions described under Par. U-142 of the ASME Code for Unfired Pressure Vessels, to qualify materials to be used in vessels for low-temperature service.

It is not within the scope of this paper to discuss the merits of the Charpy notched-bar test, as compared with other tests, from the standpoint of geometry, testing variables, etc., but rather to describe the use of the Charpy test as a means for comparing the ability of certain materials and weld metals to deform when tested at various temperature levels.

Robert W. Bennett is Chief Quality Control Engineer with the Alco Products Division, American Locomotive Company.

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Acceptance Limits

The limit of acceptance required by the ASME Code for material at a given temperature level is set at the numerical value of 15 ft-lb for a standard 10- x 10-mm specimen. There is no provision regarding the character of the fracture or whether the transition temperature of the material under consideration is gradual or abrupt.

It is therefore essential that the fabricator develop the latter data for the various materials and weld metals so that during the course of qualifying the welding by Charpy and other mechanical tests for each 200 ft. of welding or each vessel, a reasonable margin of safety will be present to meet the minimum Code requirements.

Types of Steel Utilized

To meet these service requirements, plain carbon and low-alloy steels have been used, as well as the higher alloy austenitic steels and the nonferrous alloys, depending upon the design temperature and pressure.

The steels utilized for low-temperature service are generally purchased to ASTM (or ASME) or customer specifications requiring restricted chemical analyses and mechanical properties. Thus their quality is reasonably consistent and uniform from a fabricating standpoint.

In addition to choosing a suitable type steel to meet the service temperature requirements, it is highly essential that the proper class and brand of electrodes be used to obtain the room-temperature mechanical properties and the X-ray standards for weld metal required by the ASME Code or other customer specifications.

Electrodes

Various brands of electrodes of the same AWS Class often show a variation both in their operating characteristics and in the soundness and physical properties of the weld metal they deposit. With the constant development and improvement of different brands and classes of electrodes, it is essential that these new and modified electrodes, as they become available, be investigated and compared with the properties of the elec-

trodes in current use for welding each specific type of material.

This paper, therefore, deals only with the properties of the various weld metals and base materials used in the fabrication of weldments and pressure vessels for low-temperature service and with certain tests used for their evaluation. The information contained herein is from laboratory data that have been substantiated by detailed records of the properties of the weld and base materials used in the fabrication of equipment for a number of contracts over a period of years.

MATERIALS USED IN THE CONSTRUCTION OF VESSELS FOR LOW-TEMPERATURE SERVICE

During recent years there has been an ever-increasing demand for more specific and factual data on the properties of materials for subzero service with regard to their performance in structures such as ships, bridges, aircraft, and ordnance vehicles, as well as in pressure vessels, heat exchangers and storage tanks.

Studies of materials made by different processes (Bessemer, open-hearth and electric furnace) and accompanying variations in deoxidation practice, alloy additions, rolling and heat treatment have indicated specific temperature levels below which certain ferritic steels do not retain their ductility or notch toughness.

Over a period of years the ASME Code for Unfired Pressure Vessels has recognized that many of the plain carbon steels made to restricted chemical analyses in either the open-hearth or electric furnace may be used safely for temperatures to −20° F. Below −20° F the material requires qualification by the Charpy notched-bar test. A notch toughness of at least 15 ft-lb for three specimens, at the minimum service temperature to which the vessel is designed, must be obtained.

Service Temperature Requirements

Equipment operating at −50° F or above, is generally fabricated from carbon steels properly deoxidized to produce a fine-grain microstructure. Such steels are described as being aluminum-killed or silicon-killed and aluminum-finished. For service at temperatures ranging from −50 to −150° F, vessels and exchangers have been made of the low-alloy steels containing about 0.75% each of chromium, copper and nickel (with and without cobalt) or the 2½ or 3½% nickel steels.

For certain applications, the austenitic type stainless steels of the 18% chromium – 8% nickel type have been used in the foregoing temperature ranges. In general however, the austenitic materials, copper, copper alloys and aluminum alloys are generally used below −150° F and do not necessarily require Charpy test qualification.

The nominal chemical composition and room-temperature mechanical properties of the materials generally specified for pressure vessels and heat exchangers to be used for low-temperature service below −20° F are given in Table 1.

Test Factors

Although it is outside the scope of this paper to discuss the transition temperature and other basic metallurgical aspects of the plate material, it is considered pertinent to discuss the factors that are applicable from the welding standpoint. The 1946 and 1949 ASME Code stipulates that vessels made to specifications given in Par. U-68, Section VIII, require a test plate to validate each 200 linear ft of welding or each vessel. This test consists essentially of attaching to the end of a shell seam a plate of the same heat of material and thickness as the vessel being welded, and welding it at the same time and under the same conditions as the vessel. The plate is then cut into free bend, tension and Charpy test specimens as shown in Fig. 1.

The room-temperature bend and tensile tests generally pass the minimum Code requirements without difficulty. The Charpy notched-bar tests, however, conducted at the required minimum temperature level, must be made in the heat-affected zone of the weldment as well as in the weld.

Consequently, if the heat absorbed in the base material during welding experts a deleterious influence on its metallurgical properties at subzero temperatures, a brittle fracture will result in the Charpy test, and the

Table 1—Nominal Chemical Composition and Room-Temperature Properties of Materials Used for Subzero Temperature Service

Type Material	ASTM Designation A-201 Grade A	ASTM Designation A-201 Grade B	ASTM Designation A-203 Grade D	Cr-Cu-Ni	Cr-Cu-Ni-Co
Chemical Composition					
	1″-.20	1″-.24			
Carbon, max.	2″-.24	2″-.27	.17	.12	.10
Manganese, max.	.80	.80	.80	.65/.85	.35/.55
Phosphorus, max.	.035	.035	.035	.04	.04
Sulfur, max.	.04	.04	.04	.04	.04
Silicon	.15/.30	.15/.30	.15/.30	.15/.25	.20/.35
Chromium	—	—	—	.65/.85	.65/.85
Nickel	—	—	3.25/3.75	.75 max.	.50/.80
Copper	—	—	—	.45/.65	.45/.65
Cobalt	—	—	—	—	.40/.60
Aluminum	—	—	—	—	.07 min.
Mechanical Properties					
Yield point, psi	.5 UTS	.5 UTS	.55 UTS	30,000	30,000
Ultimate tensile strength UTS, psi	55 - 65,000	60 - 72,000	65 - 77,000	60,000	60,000
Elongation, 2″ - % min.	1,750,000/UTS	1,750,000/UTS	1,750,000/UTS	25-(8 in.)	1,750,000/UTS ×.95

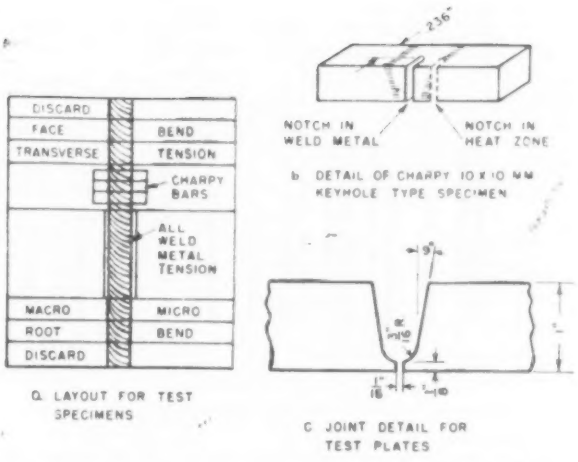


Fig. 1 Schematic drawing of joint detail and specimen layout for evaluating properties of deposited weld metal

test results will generally be below the required 15 ft-lb (for 10 x 10-mm specimens). Consistent failures of this type are generally attributed to a deficiency in the base material rather than in the welding. Check tests made from random locations of the heat and slab of plate often show a wide variation in results.

This suggests that, on the basis of the Charpy notched-bar test, the material is being tested in, or below, its transition temperature and that any of the several variables discussed at the beginning of this section, as well as the location from which the test specimens are cut from the plate, become a critical factor in the further usage of the plate.

Quality of Base Material

Plate material purchased to ASTM Specification A-300 is qualified by three Charpy notched-bar test specimens cut from one corner of each plate as rolled from a slab or ingot. (Certain customer specifications exercise more rigid control by requiring Charpy tests on each specific plate.)

It is apparent, therefore, that while the samples used to qualify the plate material at a given temperature are taken from the best practicable location without destroying the material, they may sometimes show a wide deviation from Charpy results obtained from check specimens taken from random locations of the heat (such as surplus shell plate utilized for footage qualification, nozzle cutouts, etc.).

This deviation may also be a function of cold or hot working that obtains during rolling a shell course, forming a head, or a difference in the cooling rate between the outer and inner portions of the plate.

Consequently, when a wide deviation in Charpy results is observed, it is indicative that the material is being tested at a temperature that is within the transition range. As previously stated, such a condition generally contributes to the Charpy results falling below the required 15 ft-lb minimum in the heat zone of the footage test plate and thus may render the weld and base material unqualified for Code acceptance.

Rejection of the material and 200 ft or even 10 linear ft of welding involves considerable expense as well as lost time from a delivery standpoint. The oft-repeated adage, "If we have satisfactory plate, we don't have to worry about tests in the heat-affected zones," summarizes briefly and adequately the importance of the quality of the base material from a welding standpoint in producing Code quality pressure vessels and exchangers.

INVESTIGATION OF FERRITIC ELECTRODES TO DETERMINE THE PROPERTIES OF DEPOSITED WELD METAL FOR LOW-TEMPERATURE SERVICE

For this investigation, 11 brands of electrodes were subjected to the same test conducted under closely controlled conditions. Nine brands were of the E-8015–8016 class, having a low-hydrogen line-type coating. These electrodes were dried at a temperature of 600° F for 1 hr and stored in a desiccated cabinet until they were ready for testing. This precaution was taken to eliminate any influence that coating moisture might have on the operating characteristics of the electrodes and the soundness of the deposited weld metal.

Two brands of electrodes were of the E-7020 Class, commonly termed the "hot-rod" type electrode. These electrodes, having a mineral-type coating, are not so sensitive to residual coating moisture and were therefore used directly from the shipping container.

Testing Details

Performance tests were conducted on all electrodes to determine the optimum amperage range and any variations required in operating technique. Such conditions were evaluated from bead deposit tests and welds deposited in single-vee groove butt joints. Slag coverage, bead contour, spatter, arc stability and smoke were also noted.

Initial screening tests of weld metal deposited by each electrode under optimum welding conditions were made to determine the room-temperature mechanical properties and Charpy notched-bar toughness at −150° F. One-inch thick chromium-copper-nickel steel plates having a single-U type groove joint, as shown in Fig. 1, were used for the test. Specific welding details were as follows:

1. Electrode diameter was $^{5}/_{16}$ in. except for electrode D, which was $^{1}/_{4}$ in.
2. A string-bead technique was used for the E-8015–8016 electrodes, while a normal weave technique was used for the E-7020 electrodes.
3. No preheat was used for the first pass.
4. Interpass temperatures ranged from 100 to 200° F.

5. Test plates were restrained during welding.

6. The arc was broken at random for each bead within the center third of each test plate.

7. The root side of each joint was background to clean weld metal, checked by acid etching and the weld completed.

8. The weld reinforcement was ground from both sides of the plate and the joint radiographed.

9. All test plates were stress-relieved at 1150° F for 1 hr, then furnace-cooled.

Test Results

The results of the initial Charpy notched-bar tests of the various weld metals are illustrated in Fig. 2. Since the Code requires a minimum of 15 ft-lb, for each of three 10- x 10-mm specimens, electrode brands F and L were eliminated from further testing. The weld deposited by all the other electrodes averaged 15 ft-lb or higher. Electrode K weld metal averaged 15-ft-lb, although specific values were below that figure. On this basis it was carried through further Charpy check tests as well as subjected to the room-temperature mechanical tests.

The Charpy check test data, shown in Fig. 2 and compared with the initial screening data, indicated electrodes A, B, C, D, E, H and J would be most likely to meet consistently the Code requirements. Electrode G showed one low value which indicated the possibility of inconsistent results that might obtain in the transition-temperature range. Electrode K was considered a failure and was thus eliminated from any further testing along with electrodes L and F. It was thus concluded that weld metal deposited by E-7020 electrodes was not suitable for −150° F service. (Electrodes B, H and J were received late in the investigation and thus had only one set of Charpy tests prior to making the mechanical tests.)

The mechanical properties of the weld metals were evaluated by all-weld-metal standard 0.505-in. diam tensile specimens, reduced section transverse tension

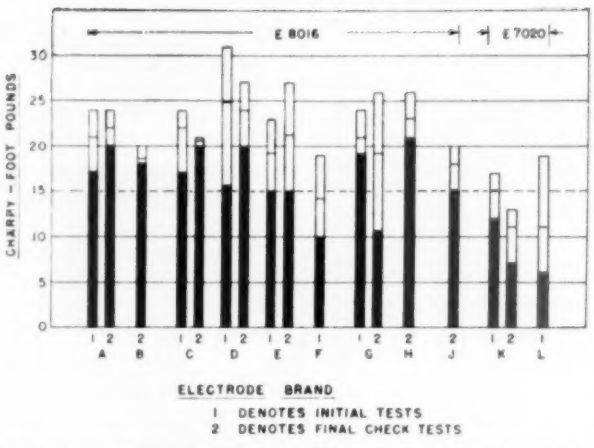


Fig. 2 Comparative results of Charpy notched-bar tests at −150° F of weld metal deposited by nine brands of E-8016 electrodes and two brands of E-7020 electrodes

tests, and root-bend and face-bend specimens tested as free bends. Standard size specimens were machined and tested in accordance with Sections VIII and IX of the ASME Code.

Specimen details are shown in Fig. 1 and test data are given in Table 2. Weld metal deposited by electrodes E, H and J failed to meet the bend test requirements of 30 elongation, whereas electrodes A, B, C, D and G passed all the room-temperature mechanical tests.

At this point in the investigation, electrode brands C and D had to be removed from further consideration because they were no longer commercially available and there was not a sufficient quantity of test electrodes to complete shop tests. This was regrettable, since all mechanical tests, Charpy tests, weld-metal soundness and operating characteristics were very good.

Shop Conditions Approximated

Many times an electrode operates exceptionally well

Table 2—Properties of Weld Metal Deposited by 11 Brands of E-8016 and E-7020 Electrodes in Cr-Cu-Ni Steel Joints

Electrode	Average Charpy Notched Bar ft-lb @ −150 F	Yield Point, psi	All Weld Metal Tensile Test: Ultimate Strength, psi	All Weld Metal Tensile Test: Elongation in 2 in., per cent	All Weld Metal Tensile Test: Reduced Area, per cent	Reduced Section Tensile Test: Ultimate Strength, psi	Reduced Section Tensile Test: Fracture Location	Bend Tests, per cent Ductility: Root Bend	Bend Tests, per cent Ductility: Face Bend	Check of Weld Metal Composition: C	Check of Weld Metal Composition: Ni	Girth Seam X-ray	Nozzle Weld X-ray
E-8015-8016 CLASS													
A	21	88,000	99,000	22.5	57.8	80,000	Plate	30.5	31.3	.11	2.30	Satisfactory	Satisfactory
B	18	74,500	89,000	25.0	61.0	79,000	Plate	35.5	40.3	.06	3.17	Satisfactory	Satisfactory
C	21	84,000	97,000	26.0	59.3	81,000	Plate	30.0	32.1	—	2.49	—	—
D	25	71,600	85,400	26.5	67.0	79,500	Weld and Plate	33.3	37.0	—	2.22	—	—
E	20	91,000	100,400	23.5	63.5	79,200	Plate	21.0	26.8	.11	1.93	Unsatisfactory	—
F	14	—	—	—	—	—	—	—	—	—	2.97	—	—
G	20	76,000	88,000	27.0	67.7	79,900	Plate	30.5	34.4	.08	2.17	Satisfactory	Satisfactory
H	23	85,000	94,000	26.5	65.8	70,900	Plate	17.8	25.3	.07	2.90	Satisfactory	Questionable
J	18	73,800	89,900	26.5	62.1	70,000	Heat Zone	15.3	30.8	.07	2.40	Satisfactory	Questionable
E-7020 CLASS													
K	13	—	—	—	—	—	—	—	—	—	1.84	—	—
L	11	—	—	—	—	—	—	—	—	—	—	—	—
Minimum required	15	—	60,000	20.0	—	60,000	—	30	30	—	—	—	—

and deposits satisfactory weld metal under the more ideal laboratory conditions but does not always prove to be so good under shop conditions. To check this shop influence, girth (circumferential) welds were made on ASTM A-106 carbon-steel pipe 18 in. in diameter and having a 0.562-in. wall. The joint detail was the same as for the test plates shown in Fig. 1, and the assembly was rotated during welding.

The welds were made in the down-hand position, using a string-bead technique with a start and stop about every 6 in. during each pass. Such a condition would be most apt to disclose a tendency for starting and crater porosity.

The root side was gouged about $^3/_{16}$ in. deep with an oxy-acetylene gouging torch, which is accepted shop practice. The weld on the root side was completed, both sides had the weld reinforcement ground flush with the pipe and the entire weld was radiographed. Electrode brands A, B, G, H and J showed satisfactory operating characteristics and satisfactory weld-metal soundness.

Electrode brand E operated satisfactorily but showed an excessive amount of slag in the weld that would have necessitated removal of most of the weld when evaluated according to ASME quality standards as given under Par. U-68. Since electrode E showed unsatisfactory bend-test results and abnormally high all-weld-metal tensile test results, this brand was eliminated from further testing.

Nozzle-weld tests were made to evaluate further the influence of shop conditions on electrodes A, B, G, H and J. ASTM A-106 carbon-steel pipe, $6^1/_2$ in. in diameter and having a 0.500-in. wall, was welded into the 18-in. pipe used for the girth weld tests. A single-vee groove butt joint was used having a $^1/_{16}$-in. root face and a nominal root gap of $^2/_{32}$ in. An internal jack further restrained the joint. The root side was back-gouged and welded as on previous tests, then the nozzle was faced flush with the shell surfaces and the deposited weld metal radiographed.

The weld metal deposited by electrodes A, B and G was satisfactory, whereas portions of the weld metal deposited by electrodes H and J contained entrapped slag. Since electrodes H and J also showed unsatisfactory bend ductility, they too were eliminated from further consideration.

Most Suitable Electrodes

On the basis of the tests described for this investigation, it was concluded that electrode brands A, B and G were most suitable for meeting the room-temperature mechanical properties and the Charpy notched-bar requirements at −150° F.

Since this investigation was completed, 43,000 lb of electrode A and about 1000 lb of electrode B have been used with consistently satisfactory results. Because of the substantial reduction in weld cutouts to remove porosity, fabricating costs have decreased. By virtually eliminating the necessity of retests to meet minimum Code requirements lost production time and testing costs have been reduced to a minimum.

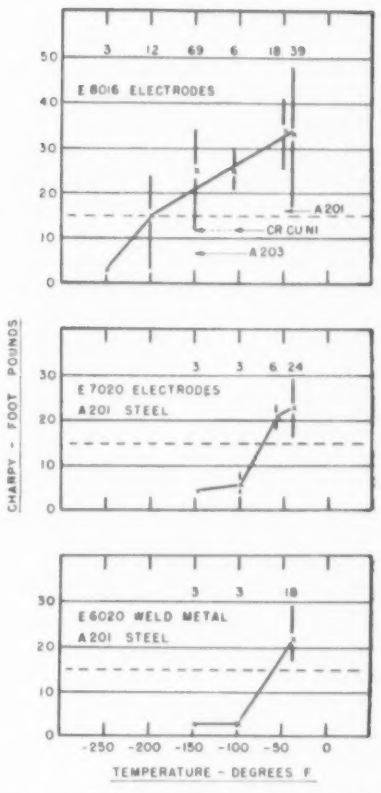


Fig. 3 Average transition-temperature curves and range of Charpy results for weld metal deposited by three classes of electrodes

CHARPY NOTCHED-BAR PROPERTIES OF WELD METAL AT VARIOUS TEMPERATURE LEVELS

The type of base material specified for a pressure vessel is dependent upon the design and temperature conditions that will be encountered in service. As the temperature limits are reduced, it becomes necessary to use materials of better quality and higher alloy. The same requirements apply to weld metal. It is obvious, however, that from a cost standpoint it is economically unsound to use the more expensive materials if the temperature or other conditions do not warrant it.

Transition-Temperature Study

A transition-temperature study was therefore made of weld metal deposited by E-6010, E-6020, E-7020 and E-8016 electrodes, using qualified shop procedures for welding A-201, A-203 and the chromium-copper-nickel low-alloy steels, to determine the most economical weld metal that would meet all Code requirements.

The term "transition temperature" has been extensively used to designate the temperature (or temperature range) at which the fracture behavior of the

test specimen changes from ductile to brittle. Various criteria, such as absorbed energy, bend angle, lateral contraction, fracture appearance, etc., have been used to evaluate the transition temperature of the weld or base material when using specimens of various sizes and designs. The ASME, however, recognizes only the Charpy notched-bar test and stipulates a notch toughness of at least 15 ft-lb at the proper test temperature.

Figure 3 shows the transition curves for weld metal deposited by AWS Class E-6020, E-7020 and E-8016 electrodes. These curves show the numerical average and range of Charpy results at the most commonly designated temperature levels. The numbers above the plotted points indicate the number of specimens represented at each test level. The E-6020 electrodes would be expected to deposit weld metal satisfactorily and consistently in killed carbon steels for temperatures to −40° F, while the E-7020 electrodes qualify for use to −60° F.

For temperatures down to −150° F, the E-8016 electrodes have showed consistently satisfactory results. While the actual transition temperature (based on 15 ft-lb) for the numerical average is somewhat lower than the −40, −60 and −150° F, respectively, a margin of safety must be used to eliminate the possibility of low scattered results (typical of Charpy tests), causing rejection of the weld metal in a fabricated vessel because of a test plate failure.

Figure 4 is a comparative summary of the average transition curves of the weld metals deposited by the E-6020, E-7020 and E-8016 electrodes and including check tests of E-6010 and submerged-arc (Oxweld No. 40) deposited weld metal.

While the E-6010 weld metal showed good comparative results to −80° F, this type electrode, because of cost considerations, is not extensively used for pressure vessels when the work can be positioned. Submerged-arc deposited weld metal is desirable from the economic standpoint, but will not meet the Charpy test requirements. This is probably due to the metallurgical characteristics of the weld metal, which are discussed later. It appears feasible, however, that an improved welding technique might substantially improve the notch toughness of the weld metal.

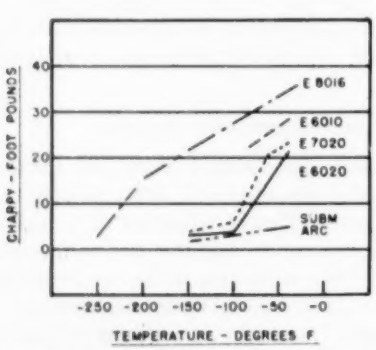
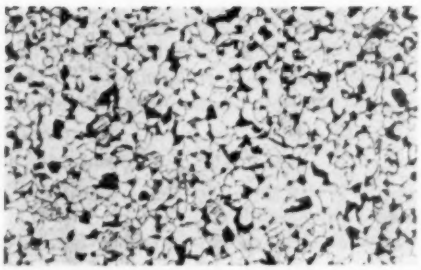
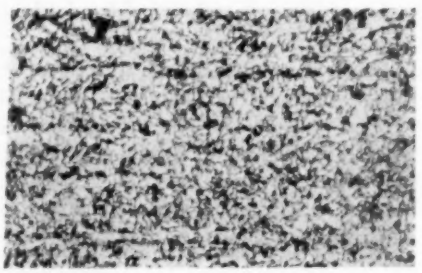


Fig. 4 A comparison of the transition-temperature curves for weld metal deposited by various classes of electrodes



a



b

Fig. 5 Photomicrographs of A-201 and A-203 steels in the as-received condition. ×100
(a) A-201 carbon steel
(b) A-203—3 % nickel steel

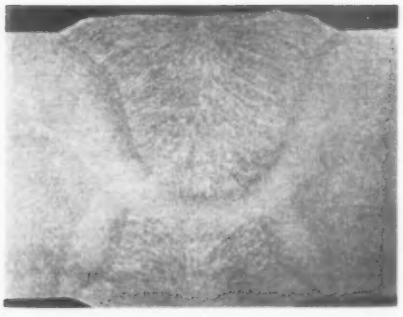
INFLUENCE OF WELD-METAL STRUCTURE ON CHARPY NOTCHED-BAR TOUGHNESS

The transition temperature of fully killed fine-grained carbon steels is generally considerably lower than that of rimmed or semikilled coarse-grained steels having the same general chemical composition. Small alloying additions of manganese, molybdenum or nickel further improve the notch toughness and lower the transition temperature below that of fine-grained carbon steels. These same factors also influence the notch toughness and transition temperature of weld metals used to join such materials.

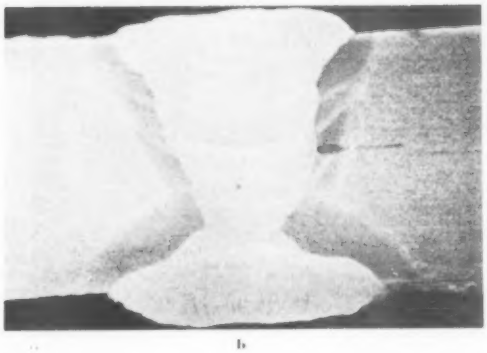
Figure 5 (*a*) shows a typical photomicrograph of the structure obtained in silicon-killed aluminum-finished carbon steel purchased to ASTM Specification A-201. Figure 5 (*b*) illustrates a typical structure of $3\frac{1}{2}\%$ nickel steel purchased to ASTM Specification A-203. Both steels are fine grained and suitable for use down to temperatures of −50 and −150° F, respectively.

Figure 6 shows photomicrographs that are representative of submerged-arc and metallic-arc deposited weld metal. The two-pass submerged-arc weld (Fig. 6 (*a*)) has a definite coarse-grained dendritic structure which is further apparent in the photomicrograph shown in Fig. 7 (*a*). Such a structure usually has a low-notched toughness which is evident from the comparative curves shown in Fig. 4.

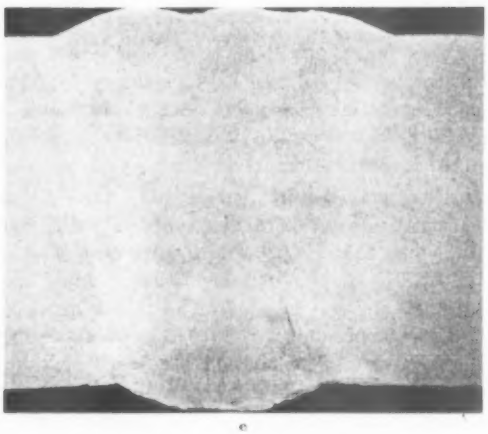
Weld metal deposited by AWS Class E-60XX, E-70XX or E-80XX metallic-arc electrodes has also shown low- or inconsistent notched toughness when a



a



b



c

Fig. 6 Photomicrographs of submerged- and metallic-arc welds in A-201 and A-203 steels

(a) Submerged arc—Oxweld 40 wire; A-201 carbon steel
(b) Metallic arc—E-7020 electrodes; A-201 carbon steel
(c) Metallic arc—E-8016 electrodes; A-203—3 % nickel steel

coarse weld-metal structure is present of the degree shown in the final top and root passes in Figs. 6 (*b*) 6 (*c*). Such a structure within the weld usually results from using large electrodes and depositing heavy passes. This condition may be improved, however, by depositing thin passes of weld metal as shown in Figs. 6 (*b*) and 6 (*c*), causing each successive bead to anneal and thus refine the grain of the previous bead as shown in Figs. 7 (*b*) and 7 (*c*).

Duplex Structure

Charpy test bars cut from an area where the weld metal has a duplex structure of coarse and fine grain generally show erratic and inconsistent results from testing. Upon developing a suitable welding technique to deposit a satisfactory weld structure, additional lowering of the transition temperature of the weld can be further obtained by small alloy additions of manganese, molybdenum and nickel as in the E-7020 and E-8016 electrodes respectively.

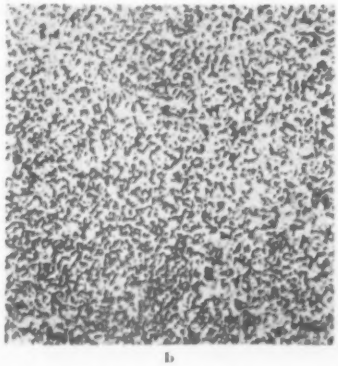
Much emphasis has been placed on the necessity of having weld metal of suitable metallurgy to meet the low-temperature test requirements so as to validate it for subzero service. However, neither the Code nor the large majority of companies using vessels for low-temperature service takes into account the as-deposited coarse-grained dendritic weld metal that comprises the final top and root passes shown in Figs. 6 (*b*) and 6 (*c*). Regardless of whether the weld reinforcement is ground flush or left intact, the coarse-grained dendritic weld metal having the low-notched toughness is so located where it is most apt to initiate failure because of the combination of low temperature, low-notched toughness, discontinuity of structure from the plate, heat zone and weld and possibly an aggravated stress condition where the weld reinforcement ties into the plate surface.

This adverse condition could be easily remedied by adding another complete weld pass, then grinding it off

Fig. 7 (Below) Photomicrographs of weld metal deposited by the submerged-and metallic-arc processes in butt joints of A-201 and A-203 steel. ×100

(a) Submerged arc—Oxweld 40 wire; A-201 carbon steel
(b) Metallic arc—E-7020 electrodes; A-201 carbon steel
(c) Metallic arc—E-8016 electrodes; A-203—3½% nickel steel

a



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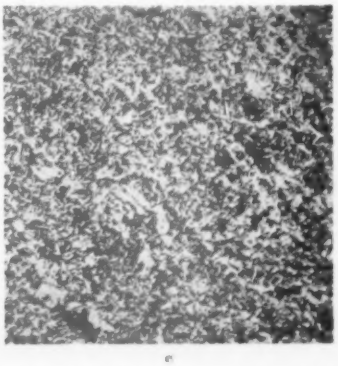

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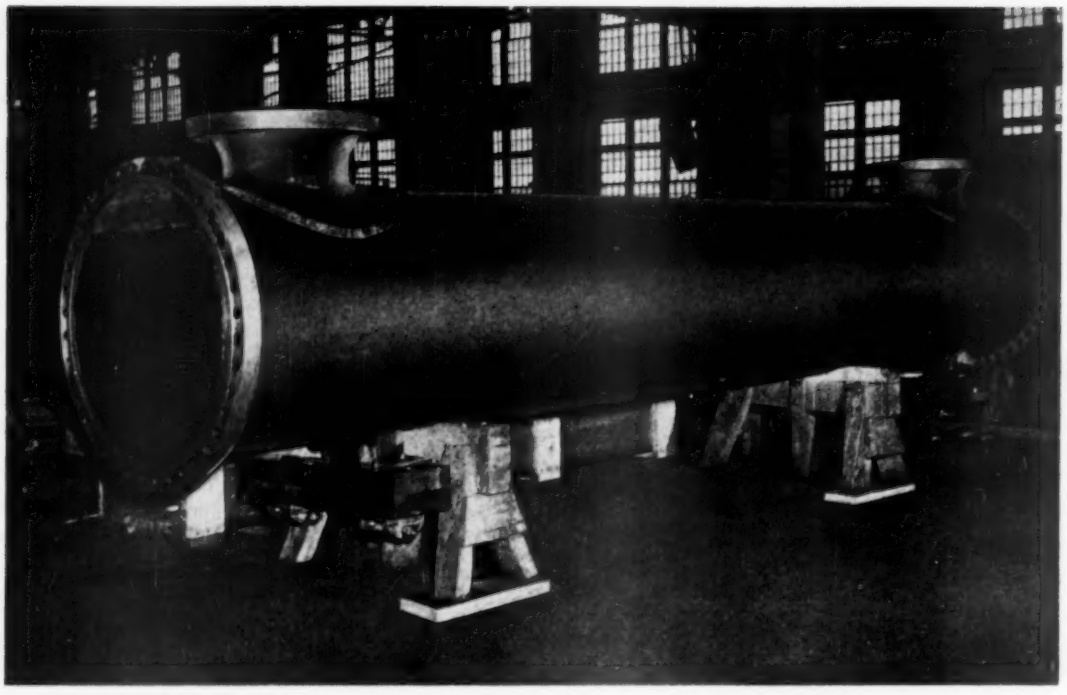

Fig. 8 Typical fixed-tube sheet type heat exchanger designed for service to −150° F

so that the outer weld surfaces would have a fine-grained structure of high-notched toughness. Grinding would also eliminate the stress raised at the intersection of the toe of the weld and the base material.

Figure 8 shows a typical fixed-tube sheet type heat exchanger, designed for service to −150° F. This exchanger is made of 3½% nickel-steel tubes and tube sheets, and welded with E-8016 electrodes.

SUMMARY

This paper discusses the properties of various weld metals and base materials used in the fabrication of pressure vessels and heat exchangers for low-temperature service. To meet these service requirements, plain carbon and low-alloy steels, purchased to ASTM (or ASME) or customer specifications requiring restricted chemical analyses and mechanical properties, are generally used. It is also essential that the proper AWS class and brand of electrodes be used to obtain suitable room-temperature mechanical properties, as well as the required low-temperature toughness evaluated by the Charpy notched-bar test.

An investigation of nine brands of AWS Class E-8016 electrodes and two brands of E-7020 electrodes was conducted to determine their stability for the fabrication of vessels and exchangers to meet the requirements set forth by Par. U-68 and U-142 of the ASME Code for Unfired Pressure Vessels for service at −150° F. Three brands of E-8016 electrodes showed consistent evidence of depositing weld metal capable of meeting all of the room-temperature mechanical and low-temperature properties, as well as satisfactory soundness and good operating characteristics. Weld metal deposited by E-7020 electrodes was definitely not suitable for −150° F service.

Transition-temperature curves of the Charpy notched-bar test data of various types of weld metals have indicated that E-6020 and E-7020 electrodes will meet all ASME Code requirements for welding killed carbon steels for service to −50° F. For service to −150° F, certain brands of E-8016 electrodes have been suitable for welding low-alloy carbon steels containing 3½% nickel or the chromium-copper-nickel steels.

Correlation of the metallurgical structure of weld metal with Charpy notched toughness indicated that a welding technique producing fine-grain weld metal is essential for optimum notched toughness. Coarse-grain dendritic weld metal generally has lower notched toughness, regardless of its chemical composition.

In considering the notched toughness of the heat-affected zone to meet Code requirements, it has been pretty well established that if the base materials are uniform and show good notched toughness at a specific temperature level, the Charpy notched-bar tests of the heat-affected zone are also generally satisfactory.

ACKNOWLEDGMENTS

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Dunkirk plant who assisted in the preparation, testing and recording of the test results. The metallographic work performed by the Metallurgical Department of the Schenectady plant is also gratefully acknowledged.

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PRACTICAL WELDER AND DESIGNER

High-Speed Welding of Nonferrous Tubing

THE hot processes of making seamless tubing in the past have been universally employed because the abrupt melting point of most nonferrous metals has precluded successful welding at speeds high enough to be economical.

The first two tube mills for converting coiled nonferrous strip at high speed into welded tubing have recently been completed by the Yoder Co. and several more are on order and being built.

The induction welding of nonferrous tubing by the new process is done at speeds ranging from 40 fpm up to 120 fpm, depending on kind of metal, gage and diameter of tube. This is from 10 to 30 times faster than practical by any method of welding heretofore known. It even compares favorably with speeds obtainable in the resistance welding of steel tubing, where the practical maximum so far has been about 150 fpm. Although resistance welding has been, and still is, the method preferred for most cold-process steel pipe and tube making purposes, the present nonferrous mills were preceded by the development by Yoder of induction-weld mills for making steel pipe and tubing up to about 4-in. diam, at speeds from 150 to 250 fpm.

The induction welding of nonferrous metals is done by high-frequency current introduced through a short inductor coil, through which the cold-formed tube passes without contact. The current travels around the surface of the tube in the region of the inductor; then flowing along the converging edges of the (unwelded) tube, to and from a nearby point of convergence, which is substantially beyond the field of induction and close to the centerline of a set of rolls which press the seam edges together.

As the current approaches the point of convergence of the edges, it is, due to the skin effect as well as the proximity effect, confined to a very shallow layer on the surfaces of the two abutting edges.

Physical tests made on samples of Type 304 stainless steel show that the tensile strength of the metal in the weld zone is about 15% higher than that of the strip from which the tubes were made.

Similar results have been obtained in tests on other

Fig. 1 Yoder nonferrous tube mill

Tube-forming mill at right. Welder with control desk to left of center. Part of sizing mill at extreme left. Strip seen entering starting table (extreme right) which embodies felt wipers, pull-over–pull-under rollers, carbide-faced side guides and a pair of adjustable edge trim cutters.

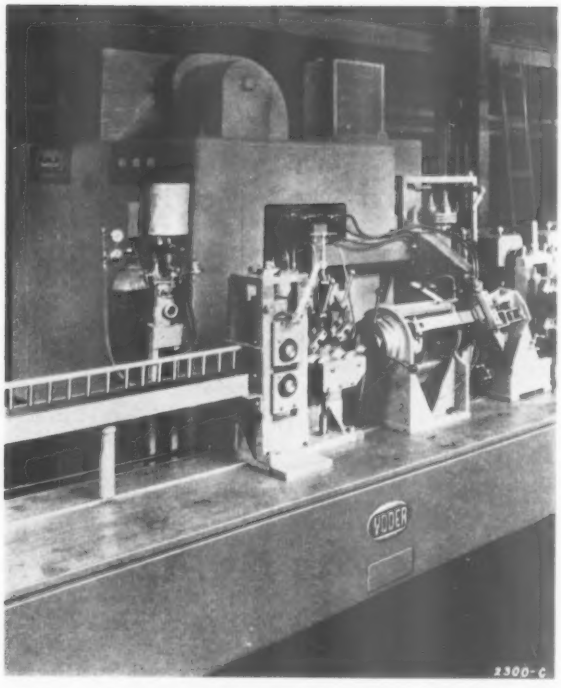

Fig. 2 Induction welder, with 50-kw oscillator in rear

Welding current introduced through short inductor coil through which tube passes without contact. On welder base are (right to left): (1) Seam guide with spreader blade for holding top gap in tubing open to predetermined width; (2) cluster of three squeeze rolls; (3) tandem weld flash cutters; (4) ironing roll stand. The inductor coil is held on arm expanding from oscillator housing, between spreader and squeeze rolls.

nonferrous metals, showing consistent weld strength, as high and higher than that of the coiled strip from which the tubing is made.

Welds in tubes made from different grades of aluminum, nickel and other metals show uniformly narrow weld zones with a grain structure almost unaffected by the heat.

The weldable metals include almost the entire range of aluminum alloys available in coiled sheet form; certain magnesium alloys; a wide range of brasses and other copper alloys; nickel and nickel alloys, such as Monel and Inconel; also austenitic and ferritic stainless steels and the lighter gages of carbon steels, the percentage of carbon being less critical than in electric-resistance and other types of welding.

In the past, steel tubing has been made in lighter gages than heretofore economically available in nonferrous tubing. Thus, steel tubing is being commercially cold formed and electric welded in gages down to 0.025 in., with a low limit of 2% applying to the ratio of wall thickness to tube diameter.

In extruded aluminum alloys, however, the practical minimum thickness has been about 0.050 in., or nearly double that of steel. Obviously, a large potential demand exists for much lighter gages, a fact which was kept in mind by the Yoder engineers in the development of the new process. The principal difficulties encountered arose from buckling of the edges of light gages in the initial cold-forming passes. Radically new methods of forming, therefore, had to be devised. Their efforts in this direction resulted in bringing the thickness-diameter ratio down to 1%, and the low limit of wall thickness down to 0.025 in. Due to this accomplishment, new marketing possibilities are opened up for light-gage nonferrous tubing.

Besides making tubing for conventional structural, decorative and fluid-conveying purposes, it also became evident that the new process is ideally adapted for making aluminum cable sheathing, a product rapidly being adopted for many types of telephone, power and bridge cable. With high speed it combines the advantage of continuous forming around cable of any desired length. In addition, the cable can be fed into the mill simultaneously with the strip, the latter being formed around it and welded, without injury to any covering of paper or rubber. In passing through the sizing mill, the sheathing is rolled down or "sized" to fit tightly around the cable. The process eliminates the separate operation otherwise necessary of pulling the cable through the finished sheathing.

The photographs show a mill built for the Bridgeport Brass Co., Bridgeport, Conn. It is designed to operate at speeds from 40 to 120 fpm, the speed depending on gage and kind of metal to be welded. The capacity ranges from 3/4- to 4-in. tubing, in gages from 0.025 in. up to 0.134 in. A similar mill has also been completed for the Kaiser Aluminum and Chemical Corp., Oakland, Calif.

A mill is now being built for the Aluminum Corporation of America. It will have a capacity up to 8 in. diam, and a mill for the U. S. Navy is to make tubing up to 5 in. diam.

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(*Continued on page 1101*)

ABC's of Pipe Cutting and Welding

by D. A. Spaulding

ALWAYS select the nozzle and oxygen pressure you would use for steel plate of the same thickness. (See the instruction folder for the blowpipe you are using.) Then adjust the preheat flame to neutral with the cutting oxygen valve open. The inner cones of the preheat flames should be from 3/16 to 1/4 in. in length when the cutting oxygen valve is open. Hold the blowpipe in your right hand to keep constant control of the cutting oxygen lever. (Fig. 1)

Balance the blowpipe on your left hand. Begin the cut about halfway between the side and top. Hold the tips of the inner cone close to the pipe and heat a spot about the size of a nickel. When the spot turns bright red and starts to melt, press down the cutting oxygen lever slowly. Wait until the cut goes through the pipe then move the blowpipe along the line of cut. (Fig. 2)

Continue the cut until you reach the top of the pipe. Keep the nozzle always pointed toward the center of the pipe. When you reach the top, stop cutting and roll the pipe so the place where you stopped is now halfway between the side and top. Then you start the cut as before. Stop and roll the pipe each time you come to the top until the cut is completed. (Fig. 3)

If the blowpipe is moved too fast the cutting jet will not go through the pipe and cutting will stop. When this happens, release the cutting oxygen lever at once. Reheat the point where the cut stopped and restart the cut. If, when you have finished, slag holds the two pieces of pipe together, separate them with a sharp rap with a hammer. (Fig 4)

All pipe should be spaced and/or beveled before welding. When pipe wall is more than 1/4 in. in thickness, it must be spaced as shown by the table below. Line up the two lengths of pipe and make several tack welds. Make the tack welds carefully so they will not be weak spots in the final weld.

Nominal Diam. in inches	*Spacing in inches for pipe beveled, single vee, 45 deg.*	*Spacing in inches for pipe not beveled*
3/4–1 1/4	Bevel not needed	3/32
1 1/2	Bevel not needed	1/8
2–2 1/2	3/32	1/8
3–8	3/16	Bevel needed
8–12	1/8	Bevel needed

D. A. Spaulding, Linde Air Products Co.

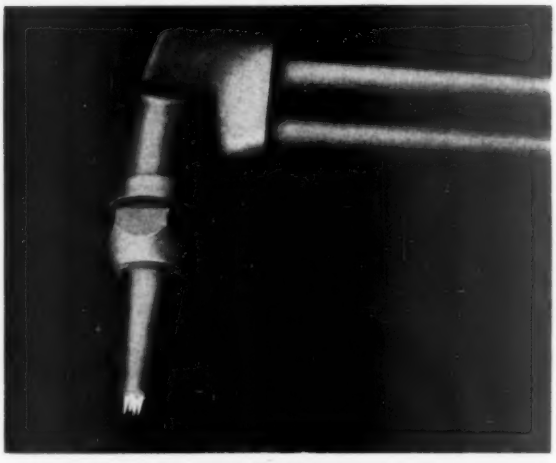

Fig. 1

Fig. 2

Fig. 3

Fig. 4

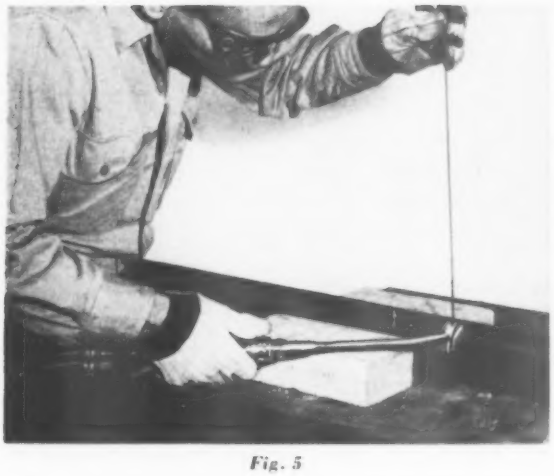

Fig. 5

Fig. 6

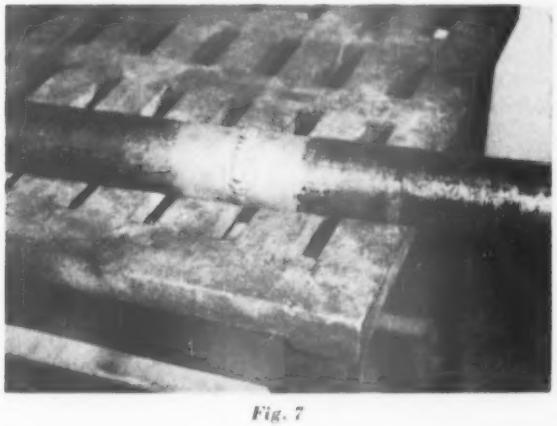

Fig. 7

Begin the final weld between two of the tack welds. Start to weld halfway between the top and side and then move to the top. When the metal melts and a puddle forms, place the welding rod in the puddle to add metal and bridge the space between the pipe ends. Hold the blowpipe so that the flame is pointed slightly upward. (Fig. 5)

Carefully move the blowpipe, rod and the puddle toward the top of the pipe. When the puddle gets near the top of the pipe stop welding. Roll the pipe so that the weld is now on the side and reheat the metal at the place you stopped welding. When a puddle forms add molten metal from the welding rod and move the weld up the pipe just as you did before. (Fig. 6)

When a tack weld is reached be sure to remelt it as the weld puddle moves along. The weld metal should be about $^1/_{16}$ in. higher than the surface of the pipe. If it is not high enough when you finish the weld, start over and deposit another layer of weld metal. Be sure to melt the first layer as you deposit the additional molten welding rod. (Fig. 7)

(*Continued from page 1099*)

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the AMERICAN WELDING SOCIETY

activities • related events

National Nominating Committee

The following have been elected members of the National Nominating Committee:

Chairman—C. H. Jennings, Engineering Manager, Welding Department, Westinghouse Electric Corp., Buffalo, N. Y.

District No. 1 (New York and New England)—L. H. Hawthorne, Welding Engineer, Revere Copper & Brass, Inc., Rome, N. Y.

District No. 2 (Mid-Eastern)—A. M. Setapen, Manager, Engineering Department, Handy & Harman, 82 Fulton St., New York, N. Y.

District No. 3 (Southern)—A. A. Holly, Executive, Arnold V. Walker Shipyard, Pascagoula, Miss.

District No. 4 (Central)—T. J. Crawford, Consulting Engineer, 3726 Kipling Ave., Berkley, Mich.

District No. 5 (Mid-Western)—T. B. Jefferson, Editor, *The Welding Engineer*, 520 N. Michigan Ave., Chicago 11, Ill.

District No. 6 (Mid-Southern)—W. J. Snider, Weld Shop Superintendent, Black, Sivalls & Bryson, Inc., Kansas City, Mo.

District No. 7 (Western)—C. W. Johnston, Service Engineer, Victor Equipment Co., Inc., 3821 Santa Fe Ave., Los Angeles 11, Calif.

1953 RWMA Prize Awards

The Jury announces the following awards:

Group 1: Industrial (10 papers entered)

1st Prize: C. A. Czohara of International Harvester Co., Chicago, Ill. Title—"Design and Application of Edge Ring Welding."

2nd Prize: G. C. Woodmancy, Boeing Airplane Co., Wichita, Kan., Title—"Balancing Inverse Parallel Connected Ignitrons in Frequency Converter Spot Welders Using Cathode-Ray Oscilloscope."

3rd Prize: Paul Klain, Dow Chemical Co., Midland, Mich.; D. L. Knight, J. P. Thorne, National Electric Welding Machines Co., Bay City, Mich. Title—"Spot Welding of Magnesium with Three-Phase Low-Frequency Equipment."

Group 2: University Staff (5 papers entered)

1st Prize: E. F. Nippes, W. F. Savage, S. S. Smith, J. J. McCarthy, G. Grotke, Rensselaer Polytechnic Institute, Troy, N. Y. Title—"Temperature Distribution During Flash Welding of Steel. Part 2."

2nd Prize: W. B. Kouwenhoven and Clarence W. Little, Johns Hopkins University, Baltimore, Md. Title—"Contact Resistance."

Group 3: University Undergraduate (2 papers entered)

1st Prize: Karl E. Dorschu, Drexel Institute of Technology. Title—"Spot and Seam Welding of Low-Alloy Steel to Stainless Steels."

Letter to the Editor

INTERNATIONAL INSTITUTE OF WELDING

Date, 14 September, 1953

Dear Sirs,

We have pleasure in informing you that, in connection with the next Annual Assembly of the IIW, the Public Session, to be held on the afternoon of 17 May, 1954 at Florence, will, in accordance with the decision of the Governing Council, be devoted to "Welding in Rail and Road Transport."

We would be obliged if you would be good enough to transmit this information to all the experts in your country who might wish to present unpublished papers on this subject.

These papers should be drafted either in English or in French and should be accompanied by a résumé in the other language and by photos and/or drawings suitable for a clear printed reproduction.

In view of the fact that little time will be available at the Public Session itself, the number of papers to be presented at the Session shall be necessarily limited and an eventual selection of the papers received will be made by a Committee of Experts in the matter.

The papers will not be read during the Session, but only discussed. They will be sent to those attending the Assembly well in advance so that members can prepare their contributions to the discussion.

In order to leave sufficient time for the printing and distribution of the papers, these must be sent *before* 15 January 1954 in two complete copies to the Comitato Direttivo dell' Istituto Italiano della Saldatura via XX Settembre N. 5/7—Genoa (Italy), which is charged with the organisation of the Public Session.

We would, however, be grateful to you if, before the papers are sent in, you would let us know as soon as you are in a position to do so, the titles of the papers to be presented and the names of the authors. This information should also be sent, like the papers themselves, to the "Comitato Direttivo dell' Istituto Italiano della Saldatura."

Thanking you beforehand for your kind collaboration, we send you our best regards and remain

yours faithfully
p. il Comitato Direttivo
dell'Istituto Italiano della Saldatura
(un Vice-Presidente)

1954 AWS National Spring Technical Meeting and 1954 Welding and Allied Industry Exposition

The AMERICAN WELDING SOCIETY announces that it will hold its 1954 National Spring Technical Meeting in the Hotel Statler at Buffalo, N. Y., Tuesday through Friday, May 4–7, 1954. The SOCIETY Headquarters Hotel will be the Statler, with guest occupancy at all other leading Buffalo hotels.

The SOCIETY also announces that it will hold its 1954 Welding and Allied Industry Exposition in the Buffalo Memorial Auditorium, at Buffalo, N. Y., Wednesday through Saturday, May 5–8, 1954. The success of the SOCIETY's first all-welding Exposition in Houston, June 1953, indicates that the Buffalo activity should be a record-establishing event. Buffalo offers an ideal spot for a welding industry convention consisting of exposition and technical meeting in that the Niagara Frontier City, the gateway to the Canadian industrial market and 75% of the industrial population of both the United States and Canada is located within a one-night train ride, and but several hours plane flight, of Buffalo.

Program for the activities is now under way. Data on the Exposition Hall facilities, floor plans, etc., will be mailed to the Industry Nov. 1, 1953. Hotel reservation forms will be mailed to the SOCIETY's members on Dec. 1, 1953. For additional information, address J. G. Magrath, National Secretary, AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

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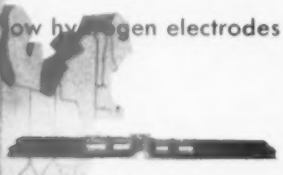

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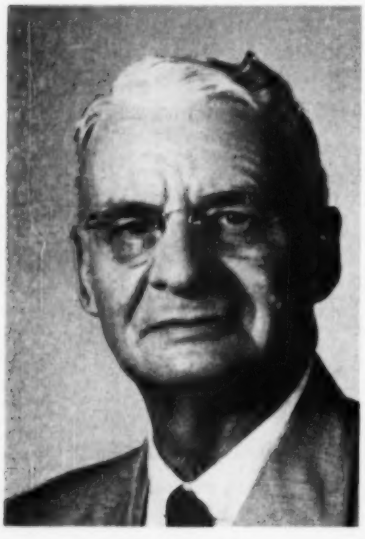
Miller Medalist—Harold O. Hill

At the Annual Dinner of the AMERICAN WELDING SOCIETY held in Cleveland, Oct. 22nd the Samuel Wylie Miller Memorial Medal and Certificate was awarded to Harold O. Hill whose unstinting devotion and wise counsel have ever been of faithful and conspicuous service in the leadership of the welding profession and in the sound advancement of the science and art of welding.



Harold O. Hill

Harold O. Hill, born in Ontario, Canada, in 1887, was educated at the University of Toronto, from which he received in 1908 the degree of B.A.Sc. in Mechanical Engineering.

He began his engineering career the same year with the Riter-Conley Co. of Pittsburgh, fabricators of plate and structural steel. Chief engineer of this company when it was merged with the McClintic-Marshall Co. in 1916, he became assistant chief engineer of the enlarged company, in charge of engineering on tank and plate work. When the McClintic-Marshall Co. was merged with Bethlehem Steel Co. in 1931, he was appointed assistant chief engineer of fabricated steel construction for Bethlehem. He has served in this capacity for 22 years.

Mr. Hill was president of the AMERICAN WELDING SOCIETY in 1947–48, and director-at-large and vice-president previously. He is chairman of the AWS Committee on Steel Storage Tanks; of the American Water Works Association Committee on Standpipes and Water Storage Tanks; of the AWS By-Laws Committee; of the American Petroleum Institute and AWS Conference Committee on Welded Oil Storage Tanks. He holds membership in these committees: AWS Welding Symbols Committee; AWS Technical Activities Committee; AWS Committee on Standard Qualifications Procedure; of the AWS-AWWA Committee on Specifications for Field Welding of Steel Water Pipe Joints.

Besides the above named organizations, he is a member of the ASTM, AIEE, ASCE and ASME. He resides in Bethlehem, Pa., and is a past president of the Bethlehem Rotary Club.

Executive Committee and Board of Directors Meetings

The third meeting of 1952–53 of the Board of Directors and the fourth meeting of 1952–53 of the Executive Committee were held in the Castilian Room A of the Shamrock Hotel, Houston, Tex., on Thursday, June 18 at 9:00 A.M. and on Wednesday, June 17th, at 9:30 A.M., respectively.

The following attended: *Chairman:* F. L. Plummer, President AWS; *Secretary:* J. G. Magrath.

Members: J. H. Blankenbuehler, J. E. Dato, R. S. Green, La Motte Grover, T. B. Jefferson, M. B. Lanier, I. Morrison, H. E. Rockefeller, J. R. Stitt and C. B Voldrich.

Guests: B. H. Allen, Chairman, Houston AWS Section; S. A. Greenberg, Secretary, AWS Technical Activities; J. F. Lincoln, President, The Lincoln Electric Co., and H. H. Wehner, Secretary, Houston AWS Section.

1. Name "Section" Changed to "Chapter"

As a result of requests from District No. 3 and other sources the Section Advisory Committee studied a suggestion that the name "AWS Section" be changed to "AWS Chapter."

Action: Upon motion, duly seconded, approval was given that the official name of a local unit of AWS be changed from "AWS Section" to "AWS Chapter." The Secretary was instructed to so advise the Constitution and By-Laws Committee, with recommendation that in that Committee's program for complete revision of the Society's By-Laws, it include the change as above so ordered.

Secretary's Notes: During deliberations of the Executive Committee, the Secretary advised the Committee as follows:

(*A*) This change would require considerable time in effecting completely all of the SOCIETY'S literature and publications, but as quickly as new literature and publications were released, or reprints made, the recommended change would be included.

(*B*) The Secretary suggested that inasmuch as a number of Sections have been successful in organizing small groups in outlying regions heretofore known as "Section Divisions," in the order of the new terminology, it would be advisable that the Constitution and By-Laws Committee, in its changing of by-laws to accommodate the foregoing, incorporate into the by-laws a provision officially allowing an AWS Chapter to organize small groups in outlying areas within the boundaries of its Chapter's activities into organizations to be known as "Chapter Sections" or "Sections" instead of as heretofore named "Section Divisions." The foregoing received the approval of the attending members of the Committee.

(*C*) The Secretary advanced the suggestion that the SOCIETY'S welfare could be furthered by providing a status for their small groups in various locations in the United States where there was insufficient potential membership for either the organization of a Chapter or a Section but which group might get together, under the leadership of the Chapter, an organization of five or more persons, such to be known as an "AWS Round-table of the AWS ———— Chapter." He stressed that these groups could hold impromptu luncheon or dinner meetings on occasions when a welding authority was visiting one of the plants of the round-table members and that such groups would form the nucleus for an eventual Section, which would in turn form the nucleus for an eventual Chapter. He suggested that this proposal be submitted to the Constitution and By-Laws Committee for its exploration. The attending members of the Committee approved of the foregoing.

2. Change "Section Chairman" to "Section President"

In the Section Advisory Committee's agenda Item C, that Committee advised that it had considered a request from the Sections to change the name of "Section Chairman" to "Section President" (in the future this request would have read, "Chapter Chairman" to "Chapter President"). After consideration the Section Advisory Committee recommended no change in the title. After discussion, all attending members of the Board and the Executive Committee were in agreement with the Section Advisory Committee's recommendation that this change should not be made.

3. Creation of New District Officer

The Section Advisory Committee advised that they had received a number of recommendations that the office of an Assistant District Vice-President be created. After study the Committee felt that a better solution would be found in reapportioning and increasing the number of Districts, retaining the pattern of one District officer, the District Vice-President, for each District. The Secretary advised that he had received other suggestions among which was the creation of the office of "District Chairman," an officer who would not be a member of

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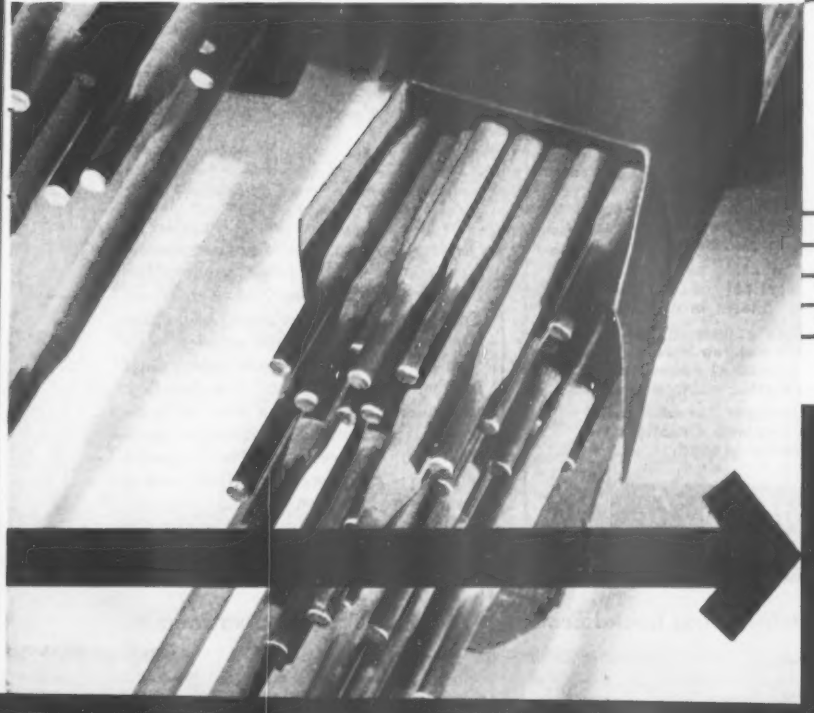

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the Board of Directors but would be an intermediate officer between the District Vice-President and the Sections' (Chapters') Chairmen, each District to have at least one District Chairman and some of the larger Districts to have two. The Secretary also advised that he had received suggestions to change the name "District Vice-President" to "District Director." Such was prompted by the fact that there was no District President and that the District officer was actually a member of the Board of Directors, nominated and elected by the District and, therefore, the title "District Director" was more appropriate and descriptive. Considerable discussion ensued regarding whether or not a solution would best be found by adding officers within the District or increasing the number of Districts, as against the advisability of increasing the number of persons on the Board of Directors. There was a wide divergence of opinion, some feeling that some of the Districts were too big for one man to cover and others feeling that regardless of the size of the District, two men could cover it no more effectively than one. The Executive Committee recommended that the title of "District Vice-President" be changed to "District Director" and that the office of Assistant District Director be created but that the District have but one representative on the Board of Directors, namely, the District Director who would have the power to appoint an Assistant District Director as his alternate. Further, the Executive Committee recommended that the organizational pattern of the Section Advisory Committee be somewhat changed to include the seven District Directors, with seven Assistant District Directors as alternates, and that the Chairman of the Section Advisory Committee be an automatic office to be held by the Second Vice-President of the Society. Some members disagreed with this pattern but the Executive Committee membership pointed out that inasmuch as there was such a wide divergence of opinion, drastic changes should not be made and it strongly suggested adoption of its recommendations. The Executive Committee believed that the problem could not be solved completely at once but that its recommendations comprehended taking the right step toward eventually finding a solution. Accordingly, it stood by its proposals. The Chairman called for a vote on adoption of the Executive Committee's recommendations.

Action: Upon motion, duly seconded, the recommendations of the Executive Committee were adopted by a plurality vote. The vote consisted of five affirmative and five negative, exclusive of the Chairman. The Chairman cast the deciding vote in the affirmative. The Secretary was ordered to report the foregoing to the Constitution and By-Laws Committee for its writing and submitting suitable by-laws covering the recommended changes.

4. Public Relations Committee

The Public Relations Committee recommended to the Board of Directors that it adopt the following resolution in order that the Society's various Committees might most effectively operate during the present epidemic of legislative action by many states on the qualification of welders:

"Under no condition shall the American Welding Society issue a certificate or license to a welding operator. The Society shall oppose any plan for the certification or licensing of welding operators that denies a fabricator the privilege of qualifying his own welding operators."

After discussion it was decided to accept the recommendation of the Public Relations Committee with modifications which would alter the text of the resolution as follows:

"Under no condition shall the American Welding Society issue a certificate or license to a welder. The Society shall oppose any plan for the certification or licensing of welders that denies a fabricator the privilege of determining his own welding procedures and qualifying his own welders."

5. Meritorious Award Certificate

As a result of previous discussion in Board of Directors' meetings and recommendations from important people in the Society, the Secretary, at a previous Board meeting, was requested to prepare a suggested plan for the creation of a special award certificate and organization for the awarding thereof. The Secretary presented the suggestion that a Meritorious Award Certificate be created which shall be given each year to a specific number of members who, either over a period of years or on a specific occasion, have made a distinct contribution in service or activity to the welfare of the Society at the *national* level; the

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3. Positioner arm swings and can be locked in proper position to either fixture.
4. 22" diameter wheel shown on fixture.
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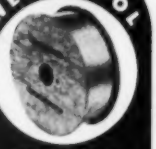

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awardees to be chosen by a new Standing Committee, known as the "Meritorious Award Committee," comprised of the Society's President, the First and Second Vice-Presidents, the Treasurer, and the immediate Past-President, if living, or his next predecessor, the last to serve as Chairman. Nominations by the Committee are subject to approval by the Board of Directors. The certificate is to be presented to the awardee by the President at the time of a National Meeting of the Society. The wording of the certificate should indicate that the award is on a basis of nomination by Committee and action taken by the Board of Directors, and to be signed by the Society's President and Secretary. Rules for procedure on awarding of the Meritorious Certificate and the wording of the certificate, as well as the wording of by-law providing for Committee, are to be prepared by the Society's Constitution and By-Laws Committee and submitted to the Board of Directors for approval at an early meeting. It was suggested that the number of these awards to be given should be left open so as to allow, during the first one or two years, an opportunity for a larger than ordinary number to be awarded in order to properly recognize a considerable number of persons who, over a period of years, have earned such award.

Action: Upon motion, duly seconded, the institution of a Meritorious Award Certificate and the creation of a standing Committee, known as the "Meritorious Award Committee," or equivalent, were approved and the Board's recommendations are to be referred to the Society's Constitution and By-Laws Committee for action in the order of the foregoing suggestions presented by the Society's Secretary.

6. *Commendatorious Award Certificate*

As a result of previous discussion in Board of Directors' meetings and recommendations from important Section Officers in the Society, the Secretary, at a previous Board meeting, was requested to prepare a plan for the creation of a special award certificate and organization for the awarding thereof. The Secretary presented the suggestion that a Commendatorious Award Certificate be created which shall be given each year to a specific number of members who, either over a period of years or on a specific occasion, have made a distinct contribution in service or activity to the welfare of the Society at the *District* and/or *Section* level. The Section Advisory Committee is to serve as the District Award Committee. Each year the District Award Committee will request the Sections in each District to nominate a specific number of eligible candidates providing résumés supporting their nominations. After review, the District Award Committee will elect a specific number to receive awards. The award is to be known as the "Commendatorious Award Certificate" and is to be signed by the President of the Society and the Chairman of the District Award Committee. The certificate is to be presented to the awardee by the District Vice-President at a regular meeting of the awardee's Section. The wording of the certificate should indicate that the award is on the basis of nomination by the awardee's Section and the District Award Committee. Rules for procedure on awarding of the Commendatorious Certificate and the wording of the certificate, as well as the wording of by-law providing for the Committee, are to be prepared by the Society's Constitution and By-Laws Committee and submitted to the Board of Directors for approval at an early meeting. It was suggested that the number of these awards to be given should be left open so as to allow, during the first one or two years, an opportunity for a larger than ordinary number to be awarded in order to properly recognize a considerable number of persons who, over a period of years, have earned such award.

Action: Upon motion, duly seconded, the institution of a Commendatorious Award Certificate and the creation of a District Committee, known as the "District Award Committee," or equivalent, were approved and the Board's recommendations are to be referred to the Society's Constitution and By-Laws Committee for action in the order of the foregoing suggestions presented by the Society's Secretary.

7. *1954 Spring Meeting and Exposition*

Chairman Plummer, acting for the Executive Committee, reported to the Board the recommendations of the 1952–53 Manufacturers and the 1953 Exposition Committees. These recommendations are covered in the minutes of the joint meeting of the Manufacturers and Exposition Committees held on Wednesday, June 17, 1953, in Houston, copies of which were submitted by mail to all members of the Board of Directors. In summary, the recommendations consisted of: (1) AWS should conduct a 1954 Spring Technical Meeting and Exposition; (2) AWS should also negotiate for an extension of its present contract with the National Metal Exposition for continuing to hold a Fall Meeting activity; (3) the suggested site of Buffalo during the week of May 3, 1954, was satisfactory to the Committees; (4) AWS Spring Meetings and Expositions should not be held in the same city during the same year as their Fall Meeting. The Executive Committee recommended acceptance of the Manufacturers and Exposition Committees' report and approval of their recommendations.

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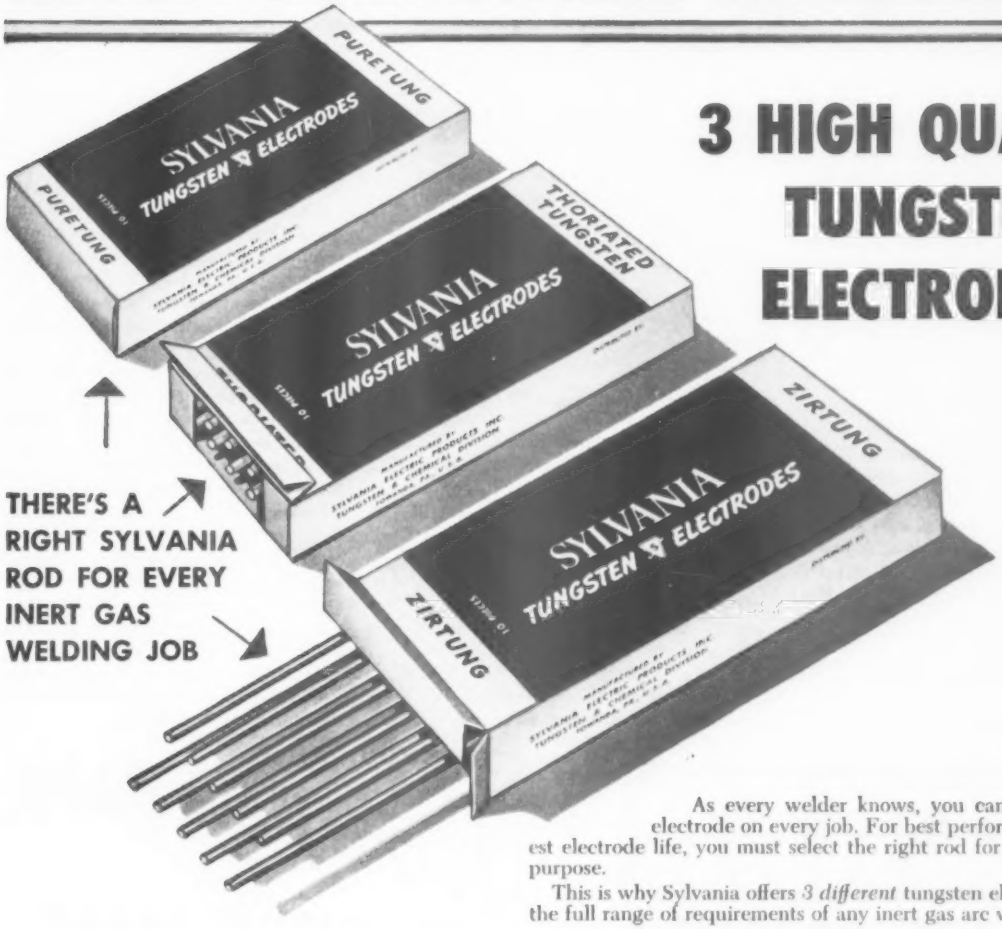

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Action: Upon motion, duly seconded, the Executive, Manufacturers and Exposition Committees' recommendations were approved.

Fourth 1952–53 Board of Directors Meeting

The fourth meeting of the AWS Board of Directors for the 1952–53 year was held on Friday, Aug. 28, 1953, at 10:00 A.M., in Conference Room 2 of the Hotel Statler, New York, N. Y., with the following in attendance:

Members: F. L. Plummer, *Chairman;* E. N. Adams, O. B. J. Fraser, O. B. Howard, C. H. Jennings, I. Morrison, I. A. Oehler, H. E. Rockefeller, J. R. Stitt and C. B. Voldrich.

Staff: J. G. Magrath, *Secretary;* and F. J. Mooney, *Assistant Secretary.*

Guests: Howard Biers, American Vice-President, International Institute of Welding; S. A. Greenberg, Technical Secretary, AMERICAN WELDING SOCIETY; H. O. Hill, Chairman, Constitution & By-Laws Committee; J. Landau, Auditor, AMERICAN WELDING SOCIETY; and H. F. Reinhard, Chairman-elect, Public Relations Committee.

1. Nominating Committee Appointment

The Chairman advised that he was recommending to the Board of Directors, for their approval, his appointment of C. H. Jennings, Past-President, as Chairman of the National Nominating Committee for the 1953–54 year.

Action: Upon motion duly seconded, the appointment of Mr. Jennings was so approved.

2. Membership Committee Appointment

The Chairman advised that he was recommending Board approval of his appointment of J. R. Stitt as Chairman of the National Membership Committee for the 1953–54 year.

Action: Upon motion duly seconded, the appointment of Mr. Stitt was so approved.

3. Memorial Resolution, G. N. Sieger

The Chairman called upon J. R. Stitt, Member of a Memorial Resolution Committee, appointed by him and approved by the Executive Committee on May 29, 1953, consisting of J. R. Stitt and Keith Sheren, to present a draft of a memorial resolution for G. N. Sieger, Past-President. Mr. Stitt, on behalf of his Committee, presented the resolution. In discussion following there were several minor word amendments suggested by the Board members which recommendations were accepted by Mr. Stitt for his Committee.

Action: Upon motion, duly seconded, the Resolution as prepared by the Memorial Resolution Committee, and as amended, was adopted.

Resolved, That the AMERICAN WELDING SOCIETY record with profound sorrow the untimely death on Apr. 1, 1953, of its Life Member and Past National President, George N. Sieger, who during the past (20 years) gave unstintingly of his time and energy to the advancement of the SOCIETY and the welding industry.

His comprehensive understanding of his fellow man, willing cooperation and gracious friendly nature endeared him to his countless friends and associates. His foresighted approach and perseverance enabled him to advance the best interests of the welding sciences in many varied ways.

During his long and colorful career he showed boundless energy and enthusiasm in the promotion, application and growth of welding in all of its phases, never forgetting the human element, thereby creating better understanding and cooperation.

The record shows that he was predominately responsible for the unprecedented growth of the Detroit Section during his many years of loyal aggressive service as Secretary, Treasurer and Chairman.

His qualities of leadership were recognized nationally and he served as chairman and member of many committees and was elected President of the AMERICAN WELDING SOCIETY for 1948–49.

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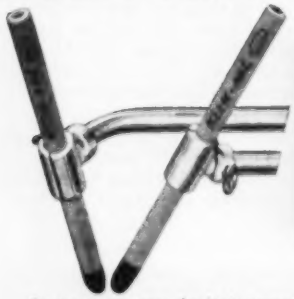

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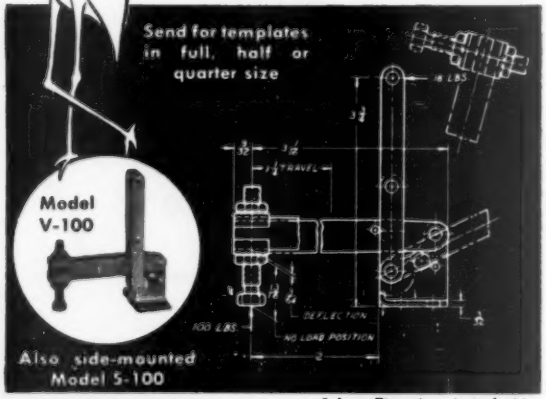

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"Now the Laborer's task is o'er,
Now the Battle's Day is Past,
Now upon the Farther Shore,
Rests the Voyager at Last,
Father in Thy Gracious Keeping,
Leave we now Thy servant sleeping."

Resolved, That the AMERICAN WELDING SOCIETY acknowledge the loss of an outstanding engineer and associate and express its sincere sympathy to the family and the many friends and colleagues of George N. Sieger.

4. Budget for 1953–54 Operation

In the absence of the Treasurer, R. S. Donald, H. E. Rockefeller, Member of the Finance Committee, was appointed by Treasurer R. S. Donald to represent the Finance Committee at this meeting and to present the Finance Committee's proposed Budget for the 1953–54 Fiscal Year, as based upon the experience of the SOCIETY's operations for the first ten months of the 1952–53 Fiscal year. Mr. Rockefeller read the Minutes of the Meeting of the Finance Committee held on Thursday, August 27th, in New York, and submitted to the attending Board Members copies of the proposed Budget for income and expenses for the Fiscal Year ending Aug. 31, 1954. The Budget was reviewed in detail and items as observed were explained by either Mr. Rockefeller, Mr. Landau, Auditor, or F. J. Mooney, Assistant Treasurer.

Action: Upon motion, duly seconded, the proposed Budget for the Fiscal Year ending Aug. 31, 1954, as submitted by the Finance Committee, was approved.

5. Membership Billing Procedure

Action: Upon motion, duly seconded, the Chairman's recommendation that a Special Committee on Membership Billing Procedure be appointed, consisting of the SOCIETY's Treasurer as Chairman, the Chairman of the National Membership Committee, the Chairman of the Admissions Committee and the Assistant Treasurer of the SOCIETY, was approved.

6. Educational Activities

As hereinbefore stated, the Budget provided an amount of $1000 to be set aside for Educational Committee operation requirements. The Secretary outlined the essentials of the Annual Report of the Chairman of the Education Committee, Julius Heuschkel. He estimated that to provide sufficient funds to the various things suggested by Mr. Heuschkel, acting for his Committee, would require funds in the order of several additional thousands of dollars. The Secretary observed that if the National SOCIETY desired an Educational Committee activity comprised of services to the undergraduate and vocational levels, a development of educational book program, etc., then the SOCIETY must recognize that such activities would cost money, and that a ways and means must be provided to support the activities of the Education Committee personnel, in view of the fact that, in the instance of educators, while their colleges or schools may allow them the necessary time for such activity, there is no way in which they can obtain funds for supporting such activity. It was agreed by the attending Members of the Board that this was an acute problem, but that the SOCIETY could only go slowly and that a sum of $1000, as provided in the Budget, would be the extent of monies available for the coming Fiscal Year. The Secretary was requested to so acquaint the Chairman of the Education Committee, to assure the Chairman and his Committee that the Board was in sympathy with their aspirations as well as the need for work done of the character recommended; was most appreciative to both the individual members and their organizations which gave them the necessary time for their activities, and to request the Education Committee to spend the $1000 this year to the best advantage for attaining some of the objectives and developing an organization and a plan of activity for the year following.

7. Headquarters Housing

The Finance Committee treated with this subject at its Meeting. The Finance Committee recommended to the Board of Directors that a special account be established for Headquarters Housing, and that 75% of the operating fund's surplus for the 1952–53 year, be transferred to this account.

Action: Upon motion, duly seconded, Finance Committee's recommendation was approved, and the Secretary was requested to reacquaint the Board of Directors with the history on Headquarters Housing, placing this item on the Agenda of the next meeting of the Board of Directors and sending out the Agenda as far in advance as possible to all Board Members, prior to the Board Meeting.

8. Special Negotiating Committee

H. E. Rockefeller, alternate member for C. I. MacGuffie, reported for the Special Negotiating Committee upon its meeting with the Officers of the AMERICAN WELDING SOCIETY and the American Society for Metals. Mr. Rockefeller outlined the arrangements made, as recommended to the SOCIETY by the Manufacturers Committee for the continuation, on the basis of contract, of AWS sponsorship and participation in the National Metal Congress and Exposition in the fall of each year. The Secretary submitted a proposed agreement which resulted from the aforesaid meeting activity and to which all attending persons of both AWS and ASM agreed. The Secretary also advised that as of August 27th he had received a telephone message from Mr. Eisenman advising that the Board of Trustees of the American Society for Metals had registered their approval of this Agreement.

Action: On motion, duly seconded, the proposed Agreement between the AMERICAN WELDING SOCIETY and American Society for Metals, covering AWS participation in the National Metal Congress and Exposition, was approved.

9. Honorary Membership, H. M. Priest

F. L. Plummer, as Chairman of the Honorary Membership and Honorary Directorship Committee, presented his

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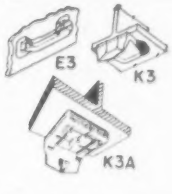

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Clip K3A permits an adjustable connection.

These widely used units eliminate all hole punching, and, with welding, produce the most economical, safe, and quickly erected structural frame.

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LET'S FACE FACTS

86¢ out of every welding dollar goes for labor and overhead. *That's* the place to start cutting costs

Check your own records. You'll find that 86% (or close to it) of welding costs go for labor and overhead, 8% for electrodes, 2% for power, and 4% for investment. As a result, every piece of welding equipment (electrodes or machines) you buy must be bought for what it does to *speed* your welding, if costs are to be cut substantially. That's exactly where Lincoln can do a job for you . . . today.

As an example, *total* welding costs for one year for a single operator and machine can be as high as $9,920. The place to start cutting costs with Lincoln welding equipment is in the approximate $8,531 that goes for labor and overhead.

Designed specifically to operate at higher currents than other rods, Lincoln electrodes enable you to *take full advantage of the extra welding capacity* . . . the extra performance built into your Lincoln machines. *Resulting cost reductions can average from 10% to 25%, including labor, overhead, power, and cost of electrodes.* "Fleetweld's" greater freedom from coating breakdown at high currents means less pin holing, less spatter . . . *top quality, high strength welds without sacrifice in speed.*

Listed at the right are E-6012 and E-6013 electrodes. Each is designed for a specific type of production job for one purpose only . . . to cut into the 86¢ out of each dollar that goes into your welding costs.

Get Lincoln's Cost Saving Facts. Let our field engineers show you how to *save hundreds of dollars each year,* per man, by utilizing cost-cutting procedures.

LINCOLN FLEETWELD Specialized Welding Electrodes for Top Speed . . . Quality Production

STRUCTURAL . . . bridges, buildings, ships.
"FLEETWELD 7" E-6012 electrode designed for speed and ease in all three positions . . . flat, vertical, overhead. High melt-off rate and low spatter loss. Exceptionally good for work where fitup is poor. For DC or AC.

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SHEET METAL . . . thin gauge steel.
"FLEETWELD 37" E-6013 for mild steel or low alloy of 3/16" thickness or less. Good build-up with little tendency for burn-through where fitup is poor. Exceptionally good slag control in all positions . . . especially in vertical down welding. For DC or AC.

HEAVY MACHINERY. . . bases, frames, columns.
"FLEETWELD 47" E-6013. A standard priced electrode with premium performance. (Most 6013-type rods are priced considerably higher.) Exceptionally smooth bead . . . welds are self-cleaning. Good control of bead shape on large fillets in all positions. For DC or AC.

HOW TO PLAN SAVINGS OF 10% TO 25% IN TOTAL WELDING COSTS

The chart at the left shows the breakdown of the welding dollar that goes into fabrication costs. As a result, it helps you *evaluate the real worth* of various welding machine features when forming your plan to cut overall costs 10% to 25%.

Labor and Overhead. By far the largest share of total cost, labor and overhead depends directly on welding manhours. Obviously the most effective way to cut welding time is to boost welding speeds, which calls for higher currents.

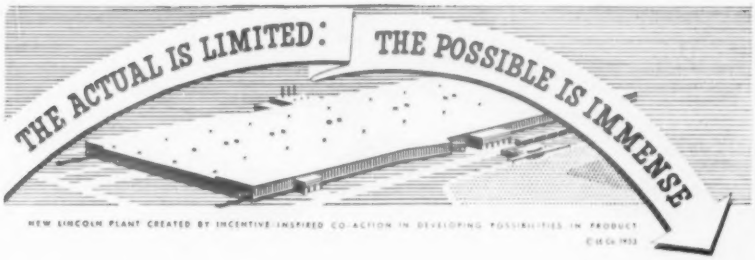
With Lincoln machines you have the *extra* reserve current capacity for these higher currents. More useable amps, completely guaranteed against burnout by thermostatic protection, enables your welders to maintain the higher speeds. But, that's not all.

Exceptional versatility in welding gives your operator ultimate flexibility in meeting every condition of weld position and type of joint. Dual Continuous Control with Job-Selector on DC Lincoln "Shield-Arc" machines gives instant selection of the right type arc as well as right arc intensity. On AC Lincoln machines, "Fleetwelder's" Arc-Booster starts the arc automatically . . . eliminates electrode sticking regardless of current setting.

Power. Only 2% of total costs go for power. Input amps for idling are, therefore, negligible, considering the added current capacity in useful welding amps that are attained through Lincoln's advanced designs of AC and DC machines.

Electrodes. Added savings in welding cost are realized through Lincoln's low electrode prices. But, more important are net benefits through higher welding speeds since "Fleetweld" rods operate at higher currents without breakdown in coating and tendency to pin hole.

Investment. Lincoln machines sell for less than other welding machines of comparable capacity to keep your investment down. However, add this to lower maintenance costs on Lincoln machines. Lincolns cost less to run because operating parts like bearings and shafts are "beefed up" to work continuously day after day without breakdown.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NEW LINCOLN PLANT CREATED BY INCENTIVE INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

AC OR DC...

It takes Lincoln's higher capacity to really lower welding costs

FLEETWELDER NEW DESIGN AC WELDER

Lincoln "Fleetwelder" built for highest speed—lowest cost manufacturing with AC.

Available in 300, 400, 500 amp sizes.

Has Lincoln pioneered Arc-Booster for instant starting arc.

Heavy duty construction with complete protection for long sustained overloads.

LEADS THE FIELD IN AC WELDERS

Still Industry's Standard for DC Welders

LINCOLN "SHIELD-ARC" WELDERS

For *greatest* reduction in welding costs

Versatile . . . has Dual Continuous Control for quick job selection.

Highest overload . . . with thermostatic protection against burnout.

Available in 200, 300, 400, 600, and 900 amp sizes.

Performance proved . . . Twice as many Lincoln DC welders are bought each year than all other DC welders combined (including rectifiers).

CHECK LINCOLN'S COST-CUTTING FACTS TODAY—*Send for Bulletin 1314 on "Fleetwelder" AC and Bulletin 459 on Linco'n "Shield-Arc" DC. Dept. 1911.*

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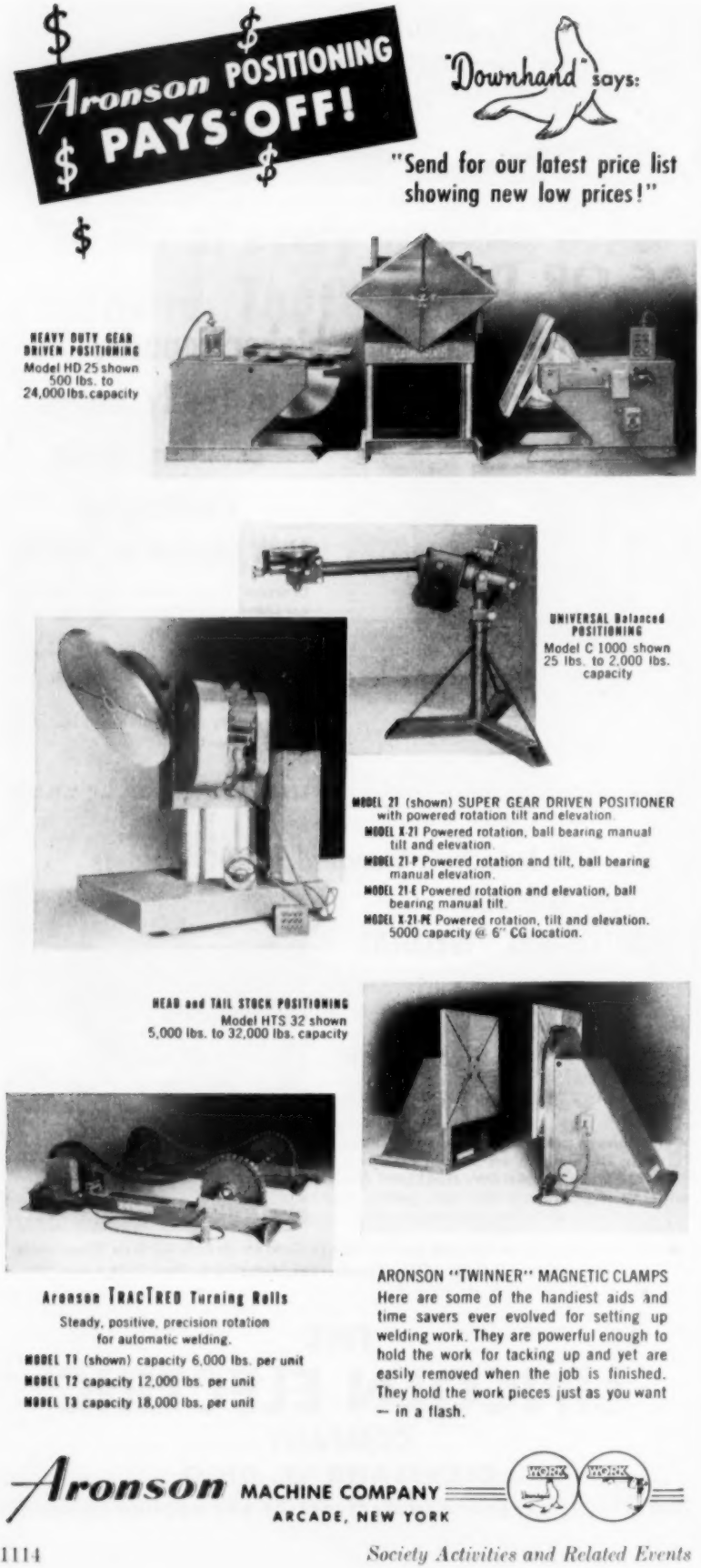
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Model HD 25 shown
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capacity
MODEL 21 (shown) SUPER GEAR DRIVEN POSITIONER
with powered rotation tilt and elevation.
MODEL X-21 Powered rotation, ball bearing manual tilt and elevation.
MODEL 21-P Powered rotation and tilt, ball bearing manual elevation.
MODEL 21-E Powered rotation and elevation, ball bearing manual tilt.
MODEL X-21-PE Powered rotation, tilt and elevation. 5000 capacity @ 6" CG location.
HEAD and TAIL STOCK POSITIONING
Model HTS 32 shown
5,000 lbs. to 32,000 lbs. capacity
Aronson TRACTRED Turning Rolls
Steady, positive, precision rotation for automatic welding.
MODEL T1 (shown) capacity 6,000 lbs. per unit
MODEL T2 capacity 12,000 lbs. per unit
MODEL T3 capacity 18,000 lbs. per unit
ARONSON "TWINNER" MAGNETIC CLAMPS
Here are some of the handiest aids and time savers ever evolved for setting up welding work. They are powerful enough to hold the work for tacking up and yet are easily removed when the job is finished. They hold the work pieces just as you want — in a flash.
Aronson MACHINE COMPANY
ARCADE, NEW YORK
WORK
WORK

Committee's recommendation that H. Malcolm Priest be appointed to Honorary Membership in the SOCIETY, with the further provision that this Award should be made in Cleveland at the National Fall Meeting of the SOCIETY in October of this year.

Action: On motion duly seconded, the recommendation of the Honorary Membership and Honorary Directorship Committee was approved, and H. Malcolm Priest was unanimously elected by the attending Members of the Board, as required by the By-Laws, to Honorary Membership in the AMERICAN WELDING SOCIETY.

10. International Institute of Welding

Howard Biers, American Vice-President of the International Institute of Welding and Chairman of the AMERICAN WELDING SOCIETY-IIW Committee, reported that the Annual Meeting of the IIW was held in July at Copenhagen this year. In total there were 328 representatives present from 18 countries. Of these, 49 delegates came from Germany, 48 from England and but two from the United States. He stressed the importance of greater activity by American industry in that American industry should interest itself in providing proper people for serving on the various IIW Commissions in manner similar to the interest exhibited by other countries and industries thereof, therefore providing permanent personnel for Commission activity. He advised that the reports of European visitors on the Mutual Security Agency Program, to our Houston activities in June past, as rendered at Copenhagen, were very complimentary. He advised that the next Annual Meeting of IIW would be held at Florence, Italy, and prior to the meeting, many of the attendees will visit, over a period of three to four weeks, various European plants. He advised that negotiations were under way with the Mutual Security Agency for establishing a "reverse" program; such to comprise the sending of American specialists to Europe for study of European production and plants, and as a result, providing more adequate assistance in that, through such visits, more will be learned regarding the facilities available to Europeans, their man power and material situation equation. He desired to know if the AMERICAN WELDING SOCIETY would favor such an activity and would welcome invitations from the IIW. The Secretary, J. G. Magrath, advised that the AMERICAN WELDING SOCIETY, had, by its continued participation in IIW, committed itself to accepting the IIW Program and now had more reason for doing so inasmuch as the American Vice-President, Howard Biers, has been nominated for the Presidency of the IIW, such office becoming open as of the Institute's next Annual Meeting at Florence, Italy. The Secretary advised that, of course, all activity must be within the

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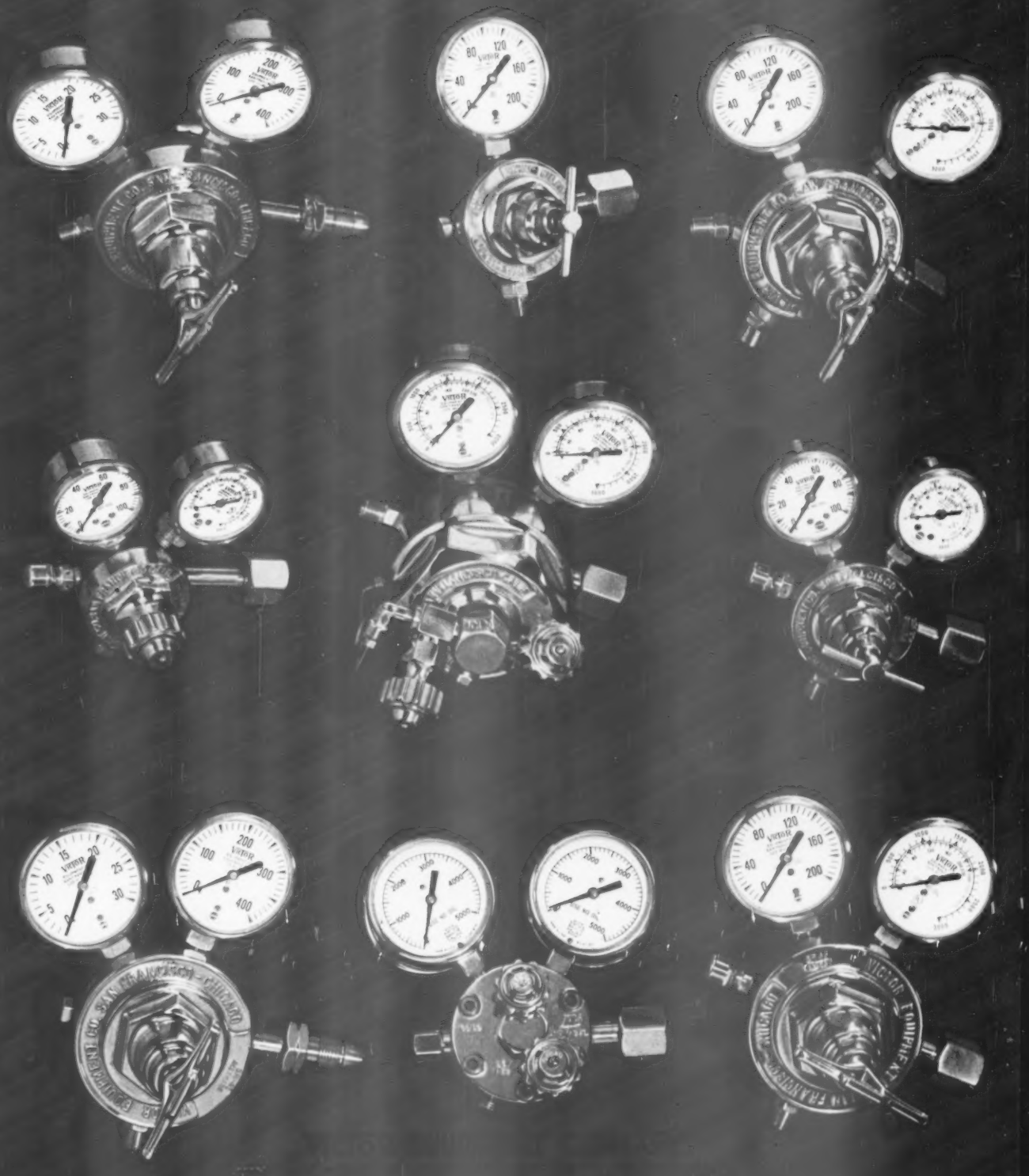

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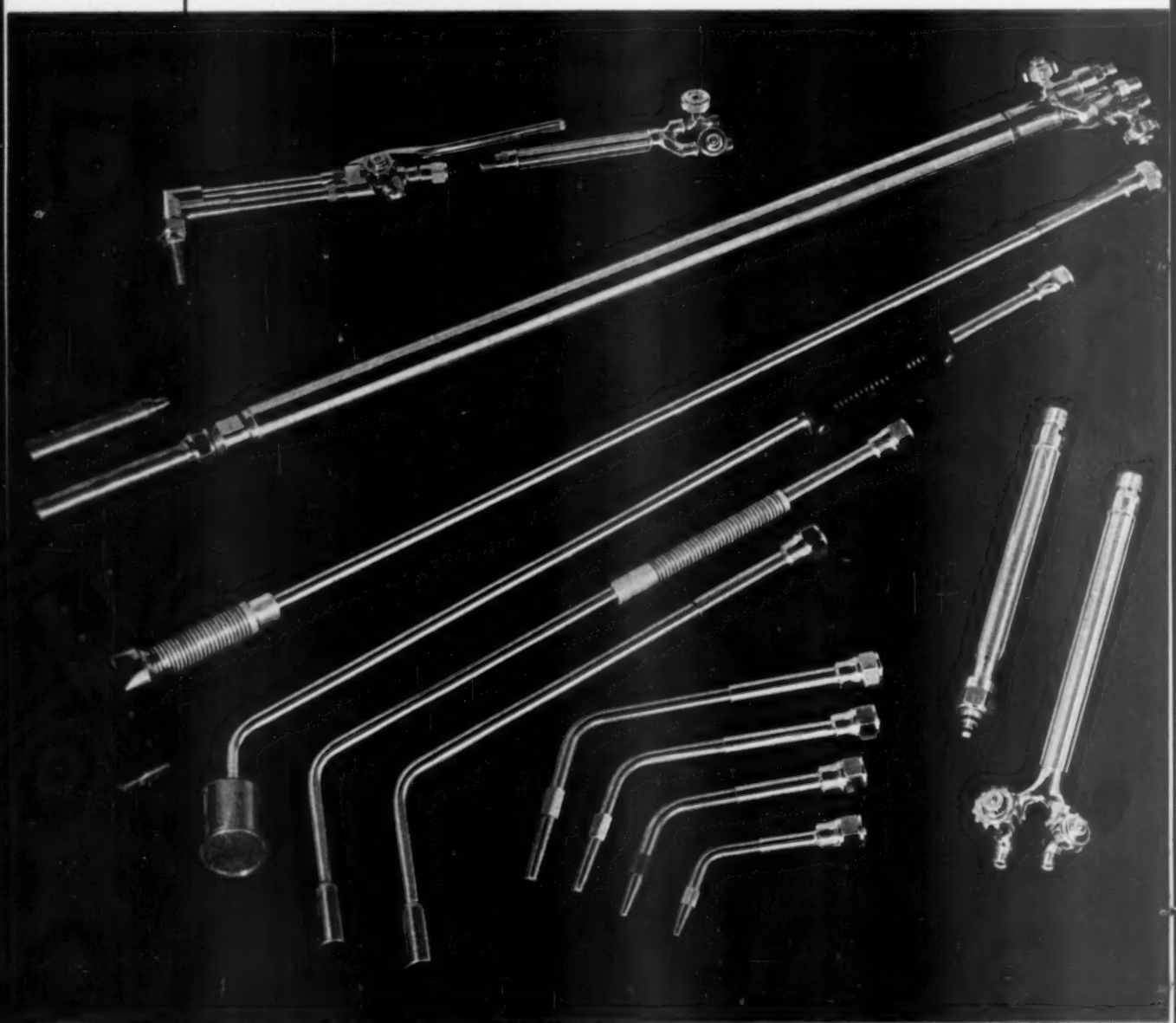

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order of the funds available within the SOCIETY, but that in accordance with past participation, we would continue to actively support the IIW Program.

H. F. Reinhard, Secretary of the International Acetylene Assn., reported on his experiences on his last trip to Europe, at which time he visited many of the welding equipment and user organizations, was well received and most impressed with the welding procedure and research work being conducted. S. A. Greenberg suggested that in selecting American authoritative personnel, medium of the Technical Activities Committee of our SOCIETY should be employed in recommendation for persons selected. Mr. Biers suggested that it was the hope of the IIW that European activities in welding, welding engineering and welding news receive greater coverage in THE WELDING JOURNAL, and he also recommended that a regular pattern be adopted of providing space on the National Meeting Programs of the AMERICAN WELDING SOCIETY for at least one European paper each meeting given by a European individual of authority invited to deliver such. Mr. Biers presented to the Board of Directors of AWS a Certificate of Appreciation from the Mutual Security Agency for the SOCIETY'S cooperation in arranging activities, plant visits and attendance at the AWS spring meeting and Exposition in Houston for the Mutual Security Agency's welding team which arrived in this country in May 1953. It was the consensus of opinion of the attending members that the recommendations made by those previously mentioned herein were of merit and that the SOCIETY should not only take more active interest, but it should enlist the active interest of American industry in the IIW activities and, further, our SOCIETY was most appreciative of the honor of the nomination of an American member of the AMERICAN WELDING SOCIETY and an active person in its organization for the Presidency of the International Institute of Welding.

Action: Upon motion duly seconded, it was voted that the AMERICAN WELDING SOCIETY take preliminary steps for more active participation in IIW through the means of promoting the interest of American industry therein, and also to the extent of recommendations to American industry, through the AWS Technical Activities Committee for the sending of industry's personnel to Europe in the order of proposed program of the Mutual Security Agency, Washington, D. C.

11. Buffalo Ordinance Report

Following a request from H. F. Reinhard, AWS Public Relations Committee, the AWS Niagara Frontier Section appointed a Local Committee which has actively investigated a proposed Buffalo City Ordinance dealing with welding and repairing of tanks which have held combustible materials; I. Morrison as Chairman. Mr. Morrison reported to the Board that his Committee was actively engaged in working in the City of Buffalo and had not this matter been called to the attention of the Buffalo AWS Officers, an Ordinance would have been written which did not mention AWS Codes. He was concerned that a similar condition might occur in many other cities when Ordinances were being instituted, and also that one city might very well depend upon the copy of another city's Ordinance which Ordinance did not observe the conditions or recommendations of AWS codes. He felt that all AWS members should be alerted to advise the SOCIETY of anything happening in their cities in the way of Ordinance adoption or institution of local codes and standards not conforming to AWS recommendations. After discussion regarding the ways and means for effecting the objective, it was decided that H. F. Reinhard, as Chairman-elect of the Public Relations Committee, should write a brief article for publication in THE WELDING JOURNAL as soon as possible, such article treating with the necessity of the foregoing. It was recommended that the attention of all technical representation of the SOCIETY, in all Sections, be turned to the article by means of a letter from National Headquarters. It was also recommended to Mr. Reinhard as Chairman-Elect of the Public Relations Committee that as quickly as possible he expand his Committee to include members who were permanently interested and active in codes, specifications and Ordinances, in the arc-

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188	338	700	1250	1800
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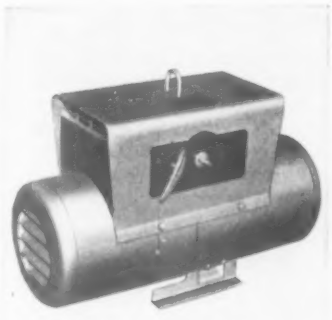

Other features:

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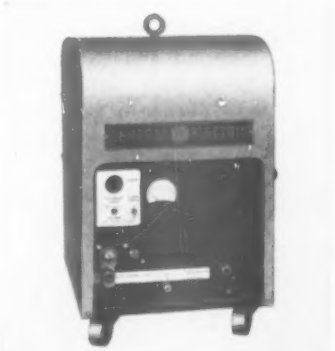

Other features:

- Dynamic braking—eliminates wire trimming before each start.
- Continuous wire-speed indication—no need to time and measure.
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Kentucky: Louisville—Reliable Welding; Paducah—Henry A. Petter Supply

Louisiana: Alexandria, Shreveport—Hughes Oxygen; New Orleans—Consolidated Welding Supplies

Maryland: Baltimore—Arcway Equipment

Massachusetts: Boston—New England G-E Welding Sales Division

Michigan: Detroit—Welding Sales & Engineering; Grand Rapids—Miller Welding Supply

Minnesota: Duluth—W.P.&R.S. Mars; St. Paul—Production Materials

Mississippi: Jackson—Jackson Welding & Supply

Missouri: Kansas City—Hohenschild Welders Supply; St Louis—Machinery & Welder

Montana: Billings—Valley Welders Supply; Butte, Great Falls—Montana Hardware

Nebraska: Lincoln—Lincoln Welding & Supply; Omaha—Baum Iron

New Jersey: Kenilworth—Welding Sales Corp.

New Mexico: Albuquerque—Industrial Supply Co., Hobbs—Western Oxygen; Las Cruces, Silver City—Car Parts Depot, Inc.

New York: Buffalo—Welding Equipment Sales; New York—Welding Sales Corp.; Syracuse—Welding Engineering & Equip.

North Carolina: Charlotte—Dixie Gases; Gastonia—Gastonia Motor Parts

North Dakota: Bismarck, Fargo—Acme Welding Supply; Fargo—Dakota Electric Supply

Ohio: Akron, Cincinnati, Cleveland, Columbus, Dayton, Mansfield—Burdett Oxygen; Toledo—Odland Iron Works

Oklahoma: Tulsa—G-E Welding Sales Division

Oregon: Eugene, Portland—J. E. Haseltine; Medford, Portland—Industrial Air Products

Pennsylvania: Allentown, Philadelphia, Pittsburgh—Arcway Equipment

South Carolina: Columbia, Greenville—Welding Gas Products

South Dakota: Deadwood—Hendrie & Bolthoff

Tennessee: Chattanooga, Knoxville, Nashville—Welding Gas Products; Memphis—Delta Oxygen

Texas: Abilene—M&M Welding Supply; Alice, Corpus Christi—Crane Welding Supply; Alpine, El Paso, Marfa, Pecos—Car Parts Depot, Amarillo, Hereford—Tex-Air Gas; Brownsville, Harlingen—Acetylene Oxygen; Dallas—Hill Equipment & Supply; Houston—G-E Welding Sales Division; Lubbock—Welders Supply of Lubbock; Midland—West Texas Welders Supply; Odessa—Western Oxygen; Pecos—Welding Supply Co.; Plainview—Plains Welding Supply; San Angelo—Southwestern Welding Supply; Snyder—Western Welding Supply; Texarkana—Hughes Oxygen; Wichita Falls—Nortex Welding Supply

Utah: Salt Lake City—The Galigher Co.

Virginia: Richmond—Arcway Equipment

Washington: Seattle, Spokane—J. E. Haseltine; Spokane, Yakima—Industrial Air Products

West Virginia: Bluefield—Bluefield Supply; Charleston—Virginian Electric; Huntington, Logan—Logan Hardware & Supply

Wisconsin: Milwaukee—Machinery & Welder

Alaska: Anchorage—Northern Supply

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welding field and the resistance-welding field, to a degree comparable to that of his interest and activity in the oxy-acetylene field.

12. Exposition Management

The Manufacturers Committee, through the medium of a Letter-Report addressed to President F. L. Plummer, from H. R. Salisbury, Chairman of that Committee, which Chairman Plummer read before the Meeting, recommended that the SOCIETY engage a professional Exposition Manager on contract, for managing the Spring 1954-AWS Welding and Allied Industries Exposition in Buffalo, N. Y.

Action: On motion, duly seconded, the Manufacturers Committee's recommendations were approved.

13. Exposition Manager

The Secretary distributed copies of proposed contract submitted by Robert T. Kenworthy covering his engagement as Exposition Manager for the 1954 AWS Welding and Allied Industries Exposition to be held at Buffalo, N. Y., in May 1954, and he advised the Board that contract was of a nature similar to that in effect for the 1953 Exposition at Houston, excepting that details were clarified to a greater extent. He stated that the Board of Directors would be required to authorize him to engage the SOCIETY in contract with such exposition manager.

Action: Upon motion, duly seconded, the Board so authorized the SOCIETY's Secretary to engage the SOCIETY in contract with Mr. Kenworthy for the 1954 activity.

14. Exposition Dates

The Secretary advised that arrangements have been made and approved by the Chairman of the Manufacturers' Committee for holding the AWS Welding and Allied Industries Exposition in Buffalo in 1954 on the days of Wednesday through Saturday, inclusive, on the dates of May 5th, 6th, 7th and 8th.

15. Spring Meeting Session Dates

The Secretary advised that he has engaged the services of the Hotal Statler in Buffalo, N. Y., for the holding of a National Spring Meeting of the SOCIETY on the days of Tuesday through Friday, inclusive, on the dates of May 4th, 5th, 6th and 7th.

16. Special Committee on Technical Services and Organization

Chairman F. L. Plummer requested the Board of Directors' approval of his appointment of a Special Committee on Technical Services and Organization; its scope being the study of the requirements of the welding products manufacturers and welding users for technical services; examination of other societies' and organizations' technical service patterns, including extent provided; and power to bring before the Board of Directors the Committee's recommendations for organizational and activity structure to be maintained and extent of services to be provided by AWS. He explained that the reasons for his recommendation were that the SOCIETY has been receiving many comments, some from its own Board Members and others from industry, which might indicate that an investigation be made in the order recommended. He also advised the Board that he would like approval of his appointment of J. H. Humberstone as Chairman of this Committee, Mr. Humbestone to recommend appointments for membership thereon.

Action: Upon motion duly seconded, the Chairman's recommendation for the appointment of a Special Committee on Technical Services and Organization; J. H. Humberstone to serve as Chairman and with the Committee's scope as recommended, was approved.

17. Awards Program

As introduced by the Chairman, the

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Secretary advised that the Staff Officers of the SOCIETY believe that a thorough study of the AWS Awards Program is due, and they suggest that a Special Committee of qualified persons be appointed for examination of existing AWS Awards Program Structure; exploration and examination of other societies' and organizations' awards patterns; with power to bring before the Board of Directors the Committee's recommendations for an awards program which will better stimulate interest in the art and science of welding, including recommendation regarding patterns of existing award programs conducted or sponsored by AWS. The Staff Officers recommend to the President and the Board of Directors that H. Malcolm Priest (present Chairman of the AWS Awards Committee) be appointed Chairman of this special Committee and that he be empowered to choose two members, same to be persons other than those presently serving on the AWS Awards Committee. President Plummer observed that this recommendation had been brought up by a number of people several times previously.

Action: Upon motion duly seconded, the recommendations of the Chairman and the Staff Officers, that a Special Committee on the Study of the AWS Awards Program, be appointed, H. M. Priest to serve as Chairman, with scope as recommended, and Mr. Priest empowered to choose two committee members other than those presently serving on the AWS Awards Committee, were approved.

18. Membership Status

The Assistant Secretary reported that as of July 31, 1953, the SOCIETY had a total registration of 9120 members consisting of 8793 active and 327 delinquent members. As of the beginning of the present membership year, namely, Sept. 1, 1952, the SOCIETY had a total registration of 7937 members, consisting of 7829 active and 108 delinquent members. Net change in this 11-month period is an increase of 1183 registered members in the order of 964 active and 219 delinquent.

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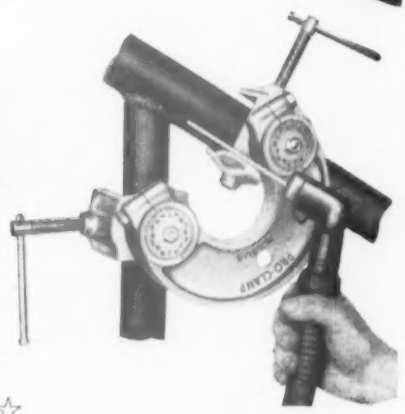

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PERSONNEL

RWMA Prize Winners

Industrial

First Prize

"Design and Application for Edge Ring Projection Welding" by C. A. Czohara has been selected as the $750 prize-winning paper in the 1953 Resistance Welder Manufacturers Prize Contest.

C. A. Czohara

C. A. Czohara was born in Gary, Ind., June 5, 1918, and graduated from the Hammond Technical High School, Hammond, Ind. He also attended the Purdue, and Indiana University Extension Center. In 1936 he started as a mechanical draftsman working up to Machine Designer. Prior to entering the military service in 1944 he became an associate consultant in the field of industrial and aircraft hydraulic transmissions. After being discharged from the service, he was employed as a Machine Designer and shortly took on additional duties as Application Engineer for a company involved in the manufacture of resistance welding equipment. For the past 3½ years Mr. Czohara has been employed by the International Harvester Co., Manufacturing Research Department, as a Welding Engineer, conducting research projects primarily involved in the resistance welding field. These research projects deal with problems on joint designs, welding procedures and techniques, redesigning for resistance welding, maintenance of equipment, etc.

Second Prize

The second industrial prize of $500 was awarded to G. C. Woodmancy of the Boeing Airplane Co. for his paper on "Balancing Inverse Parallel Connected Ignitrons in Frequency Converter Spot Welders."

Glenn C. Woodmancy

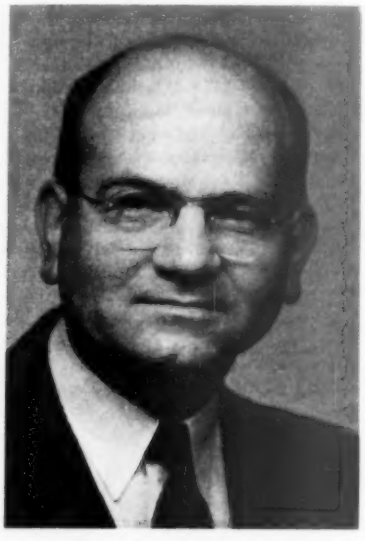
Glenn C. Woodmancy has been an engineer with Boeing Airplane Co., Wichita Division, since September 1951. His assignments since that time have all been in the Metallurgy and Welding Group of the Engineering Process Unit. His present classification in that unit is as "Associate Research Engineer" dealing with Resistance Welding problems.

Mr. Woodmancy is a 1949 graduate of Oklahoma A & M College, Stillwater, Okla., with a degree in Physics. His experience after graduation and before employment by the Boeing Airplane Co. includes one year of Physics graduate work at the University of Colorado and a year of public school teaching in Oklahoma. At the University of Colorado, he was a graduate assistant in Physics.

Prior to his World War II experience as an Air Force pilot of four-engine aircraft, Mr. Woodmancy attended Cornell University. He was born Nov. 14, 1922, at Randolph, N. Y., and obtained all of his public school education there. Mr. Woodmancy is a member of the AMERICAN WELDING SOCIETY.

Third Prize

The paper "Spot Welding of Magnesium with Three-Phase Low-Frequency Equipment" by D. L. Knight, J. P. Thorne and Paul Klain was selected for the third RWMA prize of $250.



Paul Klain

Paul Klain received his Bachelor of Science degree in Chemical Engineering in 1935 from Wayne University in Detroit. The following year, in 1936, he received his M.S. degree in Metallurgical Engineering from the University of Michigan. From 1936 to 1938 he held the position of Foundry Metallurgist with the Packard Motor Car Co. He then became associated with Battelle Memorial Institute as Research Metallurgist until 1940. Since 1940, Mr. Klain has been connected with the Dow Chemical Co. as Research Metallurgist in the Metallurgical Laboratories. During most of this time, he has worked on the research and production problems connected with the welding of magnesium.

Dean L. Knight was graduated from Michigan State College in 1941 with a B.S. degree in Electrical Engineering. Upon graduation he was employed by National Electric Welding Machines Co., Bay City, Mich., and has been in their employ ever since that time. During this period he worked in the Service and Elec-

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Dean L. Knight

trical Engineering Departments. In 1945 he became Chief Electrical Engineer and at the present time he is Chief Welding Engineer.

Paul Thorne

Paul Thorne graduated from Michigan State College with a B.S. degree in Electrical Engineering in 1943. Immediately upon graduation he entered the Army and served for four years as a radio officer with the Signal Corps, two years of which was overseas in the Pacific Theater. In 1947 he joined the National Electric Welding Machines Co. and worked in the Experimental Department on electrical design, testing and servicing of resistance-welding machines. Mr. Thorne is still with National Electric Welding Machines Co., now as Chief Electrical Engineer.

University Source

First Prize

The First prize of $300 from a University Source was won by E. F. Nippes, W. F. Savage, S. S. Smith, J. J. McCarthy and G. Grotke of RPI for the paper on "Temperature Distribution During Flash Welding."

E. F. Nippes

Dr. Ernest F. Nippes, co-author of the paper winning first prize from a university source, was born in New York City, Feb. 1, 1918. He received his Bachelor's degree in 1938, his Master's degree in 1940 and his Ph.D. Degree in 1942 all from Rensselaer Polytechnic Institute. He stayed with that Institution and served successfully as Instructor of Metallurgical Engineering, Assistant Professor and is at present Associate Professor of Metallurgical Engineering and also Director of Welding Research. He has served as Consultant with some 15 concerns, being at present with the Adirondack Foundries & Steel, Inc., Watervliet, N. Y., and the Oak Ridge National Laboratory, Oak Ridge, Tenn. His society membership includes Sigma Xi, Tau Beta Pi, Phi Lambda Upsilon, American Society for Metals, American Welding Society and American Institute of Mining & Metallurgical Engineers. He is a past chairman of the Eastern New York Chapter of the ASM, and is presently Chairman of the Northern New York Section of the AWS. He is a member of the AEC Welding Committee and, in the Welding Research Council, he is a member of the University Research Committee, the Fellowship Board, the Weldability (Metallurgical) Committee and the Resistance Welding Research Committee. He has published more than 25 papers in the field of physical metallurgy and particularly in electric arc and resistance welding.

Second Prize

The second prize of $200 from the University Source was won by the paper on "Contact Resistance" by William B. Kouwenhoven and C. W. Little.

William B. Kouwenhoven

William Bennett Kouwenhoven received his Bachelor of Engineering degree from Brooklyn Polytechnic Institute in 1906 and his Master's degree a year later. He then attended the Karlsruhe Technische Hochschule in Baden, Germany, from which he was graduated with the degrees of Diplom Ingenieur and Doktor Ingenieur. In 1914 he obtained an instructorship at The Johns Hopkins University in Baltimore, and since 1930 he has been Professor of Electrical Engineering and Dean of the School of Engineering. Dr. Kouwenhoven is a member of the American Welding Society, American Society for Testing Materials, Fellow AIEE and other societies. His principal endeavors have been in the fields of electric shock, welding and measurements. Dean Kouwenhoven is one of the co-authors of the second-prize-winning paper in the RWMA prize contest from a University source.

Dr. Clarence W. Little, Jr., group leader in the physics section of the Research Division of Allis-Chalmers Manufacturing Co., is a native of Crowley, La.

Little received his bachelor's degree in electrical engineering with honors in 1948 from The Johns Hopkins University and his doctor of engineering degree from the same school in 1952.

While doing his undergraduate work, Little was employed part-time as student assistant in the Schools of Music and Engineering at Louisiana Polytechnic Institute, as research technician by Systems Research, The Johns Hopkins University, and as night instructor in the Rehrig Radio and Television School. He was appointed junior instructor in electrical engineering for one year and instructor in electrical engineering for two years at The Johns Hopkins University, and held the equivalent of the welding fellowship for one year.

C. N. Little, Jr.

Karl E. Dorschu

Mr. Little joined Allis-Chalmers in July 1952 as a research engineer and in March 1953 was named to his present position.

Among Little's affiliations is membership in the Institute of Radio Engineers and associate membership in the American Institute of Electrical Engineers. He is 1953–54 vice-chairman of the basic sciences discussion group of the Milwaukee section of the AIEE.

Technical papers on the subjects of "Contact Resistance" and "The Spreading and Interface Resistances of Electric Contacts" have been co-authored by Mr. Little and William B. Kouwenhoven, dean of engineering and professor of electrical engineering at The Johns Hopkins University, and were presented at the 33rd National Fall Meeting of the AMERICAN WELDING SOCIETY, Oct. 20, 1952, in Philadelphia, and at the Sept. 4, 1953, American Institute of Electrical Engineers Pacific general meeting in Vancouver, respectively.

Mr. Little was married to Eleanor Dorothy MacWithey of LaGrange, Ill., on June 12, 1945. They are the parents of a seven-year-old daughter, Nancy Lee, and a two-year-old son, Jonathan Avery.

Undergraduate Prize

The $250 prize-winning paper on "Spot and Seam Welding of Low-Alloy Steel to Stainless Steels" was authored by Karl E. Dorschu.

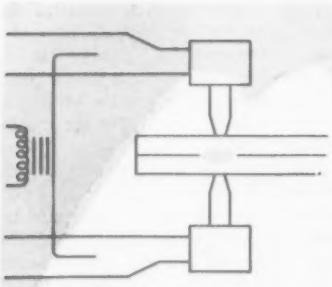
Mr. Dorschu was born in Collingswood, N. J., on Aug. 16, 1930. He graduated from Collingswood High School in June 1948, as number one in a class of approximately 245. He earned money for college by doing odd jobs while in high school. He matriculated at the Drexel Institute of Technology as a Mechanical Engineering Student in September 1948 and won a two-year scholarship at Drexel. After the first year, his interests pointed to the field of Metallurgical Engineering. Since Drexel is a school which operates under the plan of Cooperative Education, he has had three industrial jobs, chemical compounder in a plastics factory, assembly line worker in an automotive plant, and engineering trainee in Jet Engine Design Engineering at Westinghouse Electric Corp. In his senior year at Drexel, his thesis was written on "Spot and Seam Welding of Low-Alloy Steel to Stainless Steels," which won the prize. Mr. Dorschu graduated from Drexel Institute on June 13, 1953, in the upper one quarter of the engineering class. He is now employed as a design engineer at the Aviation Gas Turbine Division, Westinghouse Electric Corp.

E. L. Mills Honored

Highest honor in the Liquefied Petroleum Gas Industry, the distinguished service award bestowed annually for outstanding contributions to the industry, was recently given to Ellsworth L. Mills, vice-president of the Bastian-Blessing Co., Chicago.

The award, which carries with it an honorary life membership in LPGA, was presented by Woodward Martin of the Shamrock Oil & Gas Corp., Amarillo, Tex., a past president of the Association.

Said Mr. Martin: "Mr. Mills needs neither introduction nor eulogy to this assembly because he is the recognized founder of this association. Either personally or because of the work he has done and is doing in behalf of our industry, he is known to everyone who has been in the LP-Gas business long enough to know the difference between a pressure regulator and an excess flow check valve, whether that person lives in the United States or in the most remote LP-Gas frontier around the



Here's a Money-Saving TIP

. . . for cleaner, sounder welds—less downtime—increased production

Use AMPCO WELD® Spot-welding Tips

Get tough with your spot-welding costs. Reduce downtime, get longer runs — use Ampco Weld spot-welding tips. These electrodes have unusual resistance to mushrooming and wear. They have high electrical conductivity, too — they stay cooler, don't stick to the work.

That's why Ampco Weld resistance-welding tips require far fewer dressings, stay on the job longer, save you money because of fewer shutdowns.

Ampco-weld tips are part of an extensive line of Ampco resistance-welding products. All meet, and most exceed, RWMA specifications. And free, expert Ampco engineering service is available if you want or need it. Order Ampco tips today — and step up your production for lower costs.

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world. He is affectionately addressed as 'Colonel' throughout the industry, and properly so, for no Colonel contributed more to organizing a regiment into an effective military force than Colonel Mills has in organizing this Association into an effective force for the good of the LP-Gas Industry."

Reviewing briefly Mr. Mills' accomplishments up to the year 1927, he then continued: "Mr. Mills and his company became actively interested in the LP-Gas Industry in 1927, the year most generally acknowledged as the beginning of the LP-Gas Industry. Since then, the life of this man has been commingled with the overall progress, development and expansion of this industry to a greater degree than that of any other living individual. Few there are of us who have not solicited his and his company's aid in developing and producing some special valve, pressurestat, connection or fitting that we particularly needed, or solicited his aid in regulatory and legislative matters, or in defense of some damage suit, or other assistances too numerous to mention. The continuous flow of information from this source has contributed greatly to this industry's technical and competitive advancement and expansion; it has kept us abreast of many pertinent legislative and regulatory developments in war and peace."

E. L. Mills

He listed the Colonel's interests: "He took part in the organization of the AMERICAN WELDING SOCIETY and was on the original board of directors of that organization, helped organize and was a past president and director of the Gas Products Assn.; past president and director of the International Acetylene Assn.; director of the Agricultural Ammonia Institute, a member of the War Manpower Commission and consultant to the War Production Board in World War II."

80-Year Old Welding Research Pioneer Honored

A Press Luncheon was held October 9th at the New York Athletic Club to celebrate the birthday of 80-year old Jean Pierre H. Wasserman, president of the Societe des Soudures Castolin, Lausanne, Switzerland, described as one of the oldest living welding research pioneers, still actively engaged in industry.

He was introduced by his son Rene D. Wasserman, president of the Eutectic Welding Alloys Corp., Flushing, N. Y., who presented his father with the first medallions struck to commemorate the event.

"Coming from a miniature country such as Switzerland, to this wonderland of America," said J. P. H. Wasserman, after thanking his son for the presentation, "I never fail to be surprised at the remarkable technical progress—and fascinated by your productivity.

"My opportunities of speaking your language are limited now and with disuse has gone much of the fluency I once enjoyed. Of the 8 languages I speak, I regret to say that English has been used the least, the last few years.

"Many times when I am visiting your plants or attending your technical societies I hear words I understand, but being idiomatic the meaning is obscure. This is a handicap because in no language is so much color and descriptive detail packed into such simple phrases.

"I do not travel very extensively now. I go to Germany, France and Italy with occasional visits to Spain and I wish with all my heart that I could bring some of these people to America so that a better understanding of the American people could be reached.

"It is the half truths which hurt and mislead. They say, 'Americans have no home-life, they live in apartments.' These two statements usually go together—why I am at a loss to understand. Many

(*Continued on page 1148*)

the D-C Rectifier Welder with ARC-DRIVE CONTROL

. . . INSTANTANEOUS RESPONSE TO ARC-LOAD CHANGES
. . . INSTANTANEOUS RECOVERY
. . . REDUCED ARC BLOW
. . . COMPLETELY ADJUSTABLE BY OPERATOR

Westinghouse RA Welders with new positive arc-drive control now prevent shorting when used on "drag" welding applications. In addition, they allow complete penetration on root passes of vertical and overhead welds. Arc-drive control is obtained by adjusting the ratio of short-circuit current to welding current without changing open-circuit voltage. Actual amount of arc-drive current can be varied by the operator.

For information on this improved RA Welder or other Westinghouse Welding Equipment, write Westinghouse Electric Corporation, Welding Division, P. O. Box 868, Pittsburgh 30, Pennsylvania.

J-21607-A-2

YOU CAN BE SURE . . . IF IT'S Westinghouse

REXWELD extends SERVICE LIFE of Hartig extruder feed screws by 400%

Here is a typical case of how REXWELD Hard Surfacing Rods are used to extend tool and product life . . . to provide a better, crack and porosity free surface that wears longer.

One of the most important parts of the plastic extrusion machines made by the Hartig Engine and Machine Company is the *feed screw*. This part propels heated plastic through an extrusion die under great pressures, and at rates as high as 2000 pounds per hour. Because the slightest amount of wear on the screw is so critical — on some jobs more than 0.010" means replacement — Hartig hardfaces them with REXWELD C by the oxyacetylene process.

The Hartig people have found that REXWELD C extends the life of the screws 400% over unsurfaced screws . . . provides a surface that resists abrasion and corrosion . . . and gives them a dense facing free from surface imperfections — an important factor from the standpoint of product contamination.

Next time you have a hard surfacing job which requires the best in service life and ease of application, ask for REXWELD Hard Surfacing Rods. They are available in many different grades and sizes, and in electrodes bearing the special REXWELD low hydrogen coating.

CRUCIBLE

53 years of Fine steelmaking

REXWELD HARD SURFACING ROD

CRUCIBLE STEEL COMPANY OF AMERICA, GENERAL SALES OFFICES, OLIVER BUILDING, PITTSBURGH, PA.

REZISTAL STAINLESS • REX HIGH SPEED • TOOL • ALLOY • MACHINERY • SPECIAL PURPOSE STEELS

NEW PRODUCTS

Antiwear Aid

Answering a long-felt demand for a hard overlay which could be applied with almost "skin thickness," Eutectic Welding Alloys Corp. of Flushing, N. Y., has produced Eutec-Instant-Overlay (KR 6900) in both powder and paste form. The new product, the manufacturer claims, can actually be painted on the metal surface and, after the application of torch or carbon arc to melt the paste, bonds to the metal to form a "skin-thick" film of hard wear-resistant overlay (up to 65 Rockwell C).

"KR 6900" can swiftly and easily be applied either on new or old parts to yield increased life. Successive applications can be used to create a thicker film for even greater wear resistance.

It has obvious applications in the field of maintenance work or preventive maintenance applications on elevator steps, ramps, fork trucks and similar farm and industrial applications. In addition, the new Eutec-Instant-Overlay (KR 6900) has widespread use for application to new equipment. . .provides for increased life under abrasive conditions and other situations where a hard overlay of this type offers a solution.

Comes in regular packages of 5 lb each and a special test package of 2 lb. For further information write to Eutectic Welding Alloys Corp., Flushing, N. Y.

D-C Arc Welder

Reductions in size and weight, and increases in ease of maintenance and user and operator convenience are the principal added advantages of a new Westinghouse selenium rectifier d-c arc welder. A result of a 4-year development program, this new welder additionally incorporates all the characteristic advantages of the selenium rectifier type welder: high efficiency with low no-load losses; a-c welding performance with d-c welding characteristics; and high dependability with low maintenance due to an almost complete lack of moving parts. The first welder of this type was introduced by Westinghouse in 1949.

The heart of this new welder consists essentially of two parts: a three-phase, full-wave selenium rectifier and a so-called Transactor unit, which is a combination three-phase transformer and movable core reactor. Smaller, lighter and more convenient, the Transactor unit has two, 3-phase laminated cores. One is a fixed core on which the primary and secondary coils are wound. The other is divided into two parts—a stationary core and a movable core. The legs of the movable core are linked by the common secondary and reactor winding.

An exclusive control feature—Arc Drive Control—eliminates arc short-outs during welding applications requiring a short arc. Arc Drive Control supplies instantaneous current surges when the arc begins to short-out, thereby clearing the short and re-establishing the arc. The result is superior arc stability with improved weld quality.

Although the three standard sizes of this new welder are 200, 300 and 400 amp, Duplex models are available in 300/600- and 400/800-amp ratings.

Lightweight Welding Goggle

Dockson Corp., 3839 Wabash Ave., Detroit, Mich., announces the development of a revolutionary welding goggle of unusual design and construction that gives the wearer complete spark protection, is unusually soft against the face, covers all prescription glasses comfortably and may be raised with one hand.

Made of translucent plastic, the Dockson No. 50 Series Welding Goggle fits snugly and comfortably against any facial contour. Ample cross ventilation is provided by four vents—two top and two bottom.

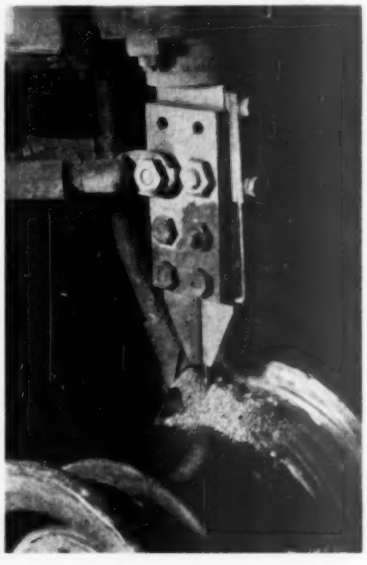
The large lens area of the Dockson No. 50 Series Welding Goggle gives the wearer greater visibility. Lense replacement is easily accomplished with Dockson push-out construction—no screws or other attachments.

Both lenses are protected by acetate covers inside and out. These lense covers reduce pitting from sparks, and eliminate any danger from shattering.

The lense holder on the Dockson No. 50 Series Welding Goggle is replaceable. Lenses are available with various shades of Federal Specification coloring.

Fluxes for Automatic Hard Surfacing

The Lincoln Electric Co. of Cleveland, Ohio, has developed a revolutionary new technique for automatic hard surfacing in which the alloy content of the deposited metal is supplied by an agglomerated granular flux rather than by the electrode. These new fluxes are an entirely new addition to Lincoln's complete line of fluxes for hidden arc welding of steel.



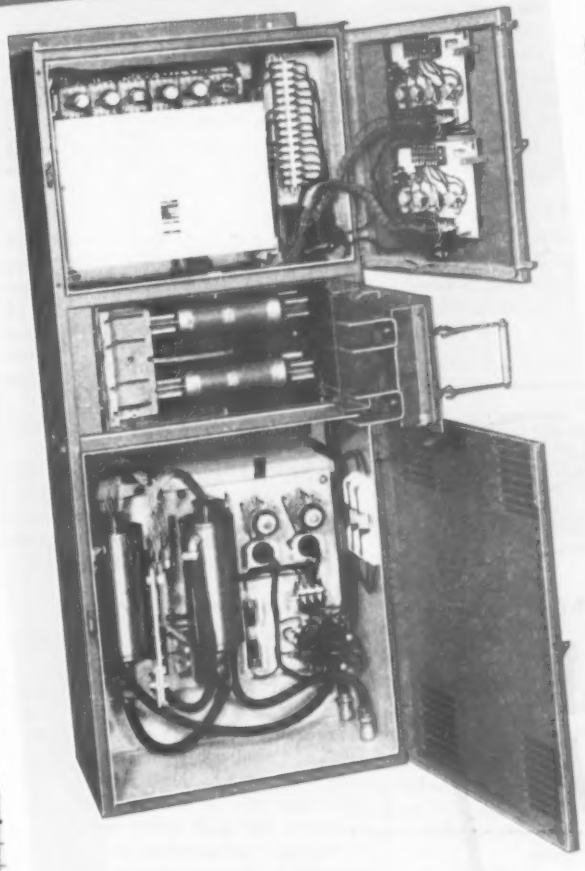
For the first time in welding history it is possible to apply automatic hard surfacing with conventional mild steel electrode simply by substituting for the mild steel flux normally used the new hard-surfacing flux. Any standard automatic welding head or Manual Lincolnweld equipment may be used without any changes; all the proved advantages of hidden arc welding such as uniform feeding and exact arc control are retained.

The company has marketed two hard-surfacing fluxes. Flux H-545 is to be used whenever a smooth layer of good abrasion resistance and high impact strength is required. It is used for deposits to be forged, such as plow share blanks; for metal to metal wear, such as tractor rollers or crane wheels; or for smooth deposits without porosity and underbead cracking, such as steel mill roughing rolls which do not require machining.

The H-545 weld metal is an alloy of carbon, manganese, silicon, chromium, molybdenum and vanadium. The de-

faster!

WELDING PRODUCTION ON REPEAT OPERATIONS



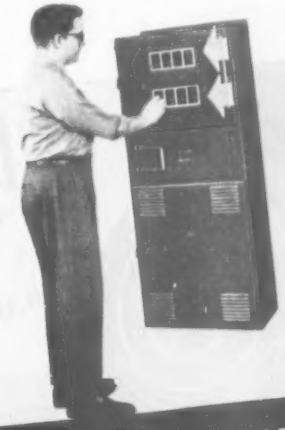
Square D's *High-Speed* Electronic Welder Control

faster! More spot welds per minute for repeat gun or machine welding operations . . . obtained by providing an additional SQUEEZE DELAY timing and a negative HOLD time sequence.

more consistent welds! Synchronized firing of ignitron tubes (welder transformer) and full cycle conduction provides uniform heat for each weld. Vastly improves quality of work.

less maintenance! Control unit uses heavy duty relays and components specifically designed for high speed welding service. Minimizes down time.

plus safety! Square D Fail-Safe tube circuits do not require conduction of an electronic tube to stop the weld . . . less possibility of danger to the operator and damage to the machinery.



dual
SEQUENCE ADJUSTMENTS
. . . provide complete control of two welding guns operated from one welder transformer and controller.

Write for High Speed Welder Control Bulletin #8992. Square D Company, 4041 North Richards Street, Milwaukee 12, Wisconsin.

SQUARE D COMPANY

1903 • **50** YEARS OF DESIGN LEADERSHIP • 1953

posit is magnetic at room temperature and with recommended procedures has a consistent hardness of 53–57 Rockwell C.

Flux H-550 is used for extremely abrasion-resistant deposits with good impact strength, such as crusher rolls, crusher liners, scraper blades, etc.

The H-550 weld metal is an alloy of carbon, manganese, silicon chromium and molybdenum. The deposit is magnetic, semiaustenitic at room temperature and with recommended procedures has a consistent hardness. A broad range of 36–62 Rockwell C hardness can be developed. Deposit is nonmachinable.

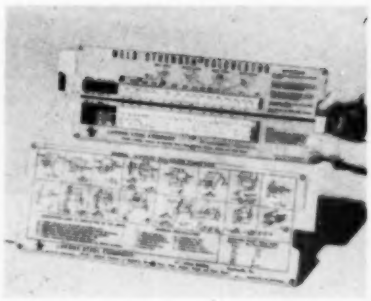
The new fluxes are available in standard 100-lb bags.

These electrodes are available from the Lincoln Electric Co. in sizes ranging from $^{7}/_{32}$ to $^{3}/_{16}$ in., in coil form for continuous automatic operations.

Weld Strength Calculator

A unique weld strength calculator has been made available by Lukens Steel Co., Coatsville, Pa., for the convenience of designers, engineers and others interested in steel plate fabrication.

A companion tool to Lukens' plate size selector, the weld strength calculator is a durably made plastic slide rule that indicates both the size of weld required for a given applied load, and the weight of

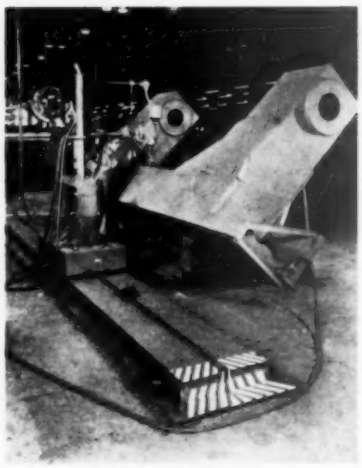


a given length of weld in pounds. The calculator gives values for stresses ranging from 2000 to 20,000 psi, and applied loads of from 9000 to 450,000 lb.

On the reverse side of this pocket-sized calculator, basic design data for welded connections are graphically shown, and formulas for calculating nominal properties of welded connections are listed in convenient form. This new weld strength calculator is available from Lukens' marketing service department.

Automatic Welding Machine

A new, versatile and simple automatic welding machine has been developed by welding engineers in the Torrance, Calif., plant of the National Supply Co. to speed up production and improve the quality of welds on large structural parts. It is



shown here welding the frame for a Hufford stretch press.

The new welding unit is a fully automatic submerged-melt welding machine incorporating a standard welding head from an older type machine. Base and track assembly is adjustable for fast alignment with the work. Track sections may be added for welding longer workpieces.

Welding head position is adjustable vertically and laterally on the carriage, and angular positions are provided by hinging the welding head on the end of the horizontal supporting arm. Welding speed is controlled by varying the speed of the carriage. Two 600-amp welding generators supply current to welding wire $^{3}/_{32}$ to $^{1}/_{4}$ in. in diameter. Flux is fed by compressed air.

The new machine multiplies the volume of work of a man with manual welding are approximately four times. Weld appearance is greatly improved and the possibility of defects is virtually eliminated.

Spot Welder

The development of a new high-precision electronic spot welder has been announced by the Unitek Corp., 275 N. Halstead Ave., Pasadena 8, Calif. This new instrument, designated the Model 1015 Unimatic Weldmaster, is designed specifically to meet production engineering needs for a more efficient and accurate method of joining light-gage metals and wire. It is especially engineered for production application in the fast growing instrument, electronic and precision components industries.

The Unimatic Weldmaster is a highly versatile, compact unit capable of performing an unequalled range of light metal joining operations. Equally satisfactory results are achieved whether the welding requirement calls for the fusing of two 0.0008-in. diam copper or nichrome wires . . . or two sheets of 20-gage mild steel sheet. The extremely low inertia head assures successful welds on copper and other materials generally considered impractical for spot welding.

AT THE FRONTIERS OF PROGRESS YOU'LL FIND AN AIR REDUCTION PRODUCT

For Welding and Cutting . . .

NATIONAL CARBIDE

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HIGHEST QUALITY
DUST FREE

Write for the name and address of the NATIONAL CARBIDE supplier nearest you.

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A DIVISION OF AIR REDUCTION COMPANY, INCORPORATED

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SPECIALIZED WELDING GUNS FROM ONE PROGRESSIVE STANDARDIZED MODEL

PROGRESSIVE'S EXCLUSIVE NYLON GUIDE

- Constant guide and support throughout stroke
- Self-insulating
- Resists weld spatter
- No lubrication required

Both Progressive guns in the above illustration have the same chassis—the difference is in the interchangeable cylinders, jaw extensions, electrode adapters, and tips.

Using Progressive's original Standardized Chassis and interchangeable parts, job-specialized portable welding guns can be made for a fraction of the cost of specially-designed guns.

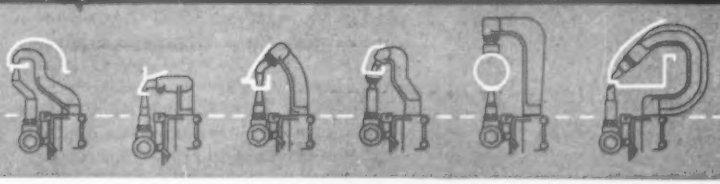
Progressive has more portable guns in use than all other manufacturers combined. For information on any type of Special or Standard Portable Gun—write to Progressive in care of Department G.

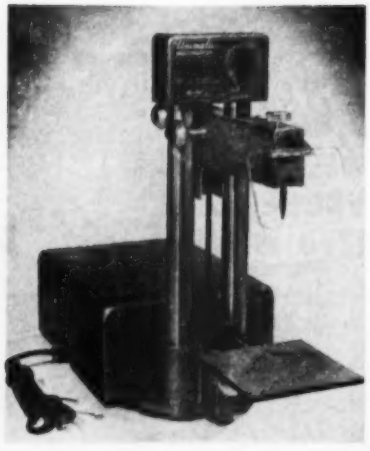
IT PAYS TO WELD

The PROGRESSIVE Welder Sales Company

3070 E. OUTER DRIVE • DETROIT 34, MICHIGAN

Group of special jaw designs demonstrating wide versatility of Progressive Welding Guns.





Where special production requirements demand precision welds in hard-to-get-at spots, the Weldmaster can be converted in seconds to a tweezer-type operation.

Unimatic's electronic circuitry is based on the capacitor discharge principle. This means the power unit stores a preset load of energy which produces a measured amount of heat when the welder is triggered. The Weldmaster, therefore, produces welds of extraordinary uniformity, precision and strength . . . showing little evidence of excessive heat or discoloration.

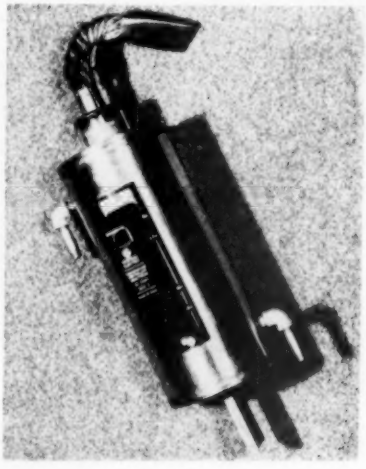
New Pressure Testing Machines

A new line of vertical pressure testing machines that detect and locate leaks in castings as well as brazed, welded and soldered assemblies is announced by Hautau Engineering Co., 721 Wanda, Ferndale 20, Mich. These air-operated, electrically controlled machines subject parts to a 10-sec automatic check in which the part or a specified area in the part is charged with a given volume of air. Loss of air pressure, indicating a leak or flaw in the part or various areas in the part is indicated by signal lights through an extremely sensitive measuring system.

Welding Control Tubes

Three new types of temperature-controlled ignitron tubes for welding control, incorporating "the first major improvement in ignitrons in nearly two decades," will be introduced by the General Electric Tube Department at the National Electronics Conference in Chicago next week, it was announced recently by J. Milton Lang, general manager of the Tube Department.

The new ignitrons' temperature-control feature is designed to effect cooling water savings up to 95% as well as to protect the tubes and the welding transformer and electrodes against overloading and overheating.



The tubes are types GL-6346 (Size B), GL-6347 (Size C) and GL-6348 (Size D). They are basically standard mercury-pool, stainless-steel-jacketed ignitrons, with addition of the temperature control device. They directly replace all corresponding ignitrons in welding control applications.

Two snap-acting switches, mounted on a constant-length reference strip, are main components of the temperature-control unit. One switch operates a solenoid valve in the water supply line to maintain the proper amount of cooling water required by the operating conditions.

The other switch, set to operate at a few degrees higher temperature, cuts off power to the tube in case of overheating or overloading.

The temperature control feature eliminates water-flow relays, water overtemperature relays, and water-pressure interlocks. Since the average water temperature is kept above the dew point under all ordinary conditions, moisture condensation and water drip on equipment are prevented.

Two Welding Machines

Morton Special High-Production Capacitor Can Welding Machine pictured in Fig. 1 is a new design approach to contour welding. A twin-station setup is used to enable the one station to be welding while the other is being unloaded and loaded.

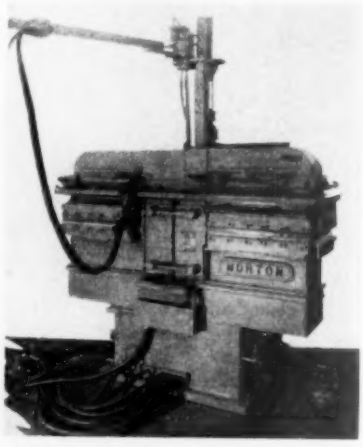


Fig. 1

Independent speed regulation at the corners insures continuous constant velocity welding over the entire butt seam of the capacitor can. Complete automatic control of the welding cycle is incorporated with rapid traverse of the welding torch carriage when moving from one end to the other.

Other features include the use of special fluid and electrical rotary valves to allow continuous rotation of the welding torch and power ejection of the welded part. Throughout the design high-unit clamping pressure is used to insure proper contact of the material to water-cooled copper backing bars.

Morton Special Fully Automatic Four-Head Submerged-Arc Welding Machine designed for welding a complete trailer body is shown in Fig. 2.

A subassembled platform consisting of three separate sheet steel parts are power conveyed to positive stop locations, follow-

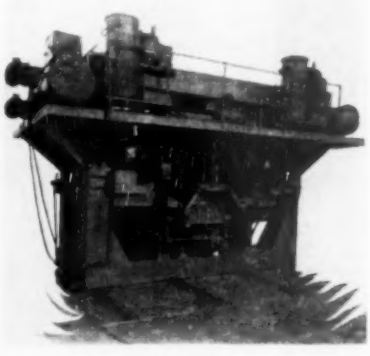
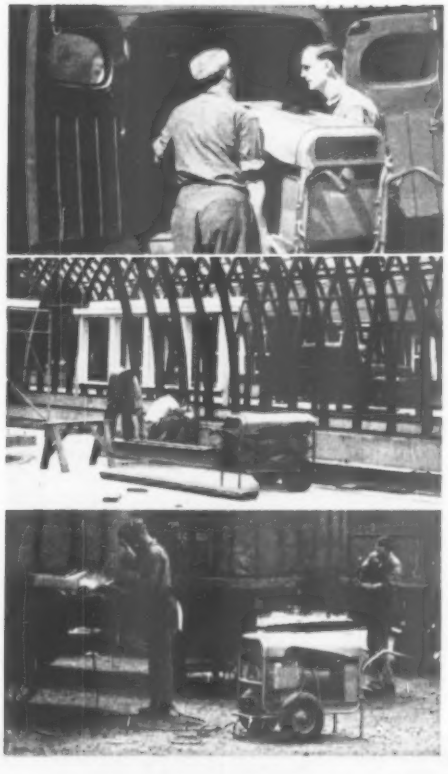


Fig. 2

MORE WELDING AT LOWER COST

with Sureweld® Portable D-C Arc Welder



The Sureweld Portable Gas-Driven D-C Arc Welder is truly an amazing welder. Amazing because it is so small and light and portable. Amazing because it can get in where other welders cannot go. Amazing because you can do far more jobs *faster* with it than any other welder of its capacity.

One man can move it. Two men can lift it. Any auto, truck or station wagon can stow it. On the job it produces welding power at once and it handles 85-90% of all on-the-job welding. Most amazing of all, it saves so much, it repays its cost several times a year.

On multi-story building the Sureweld Portable moves easily from floor to floor with the operator — always within standard cable length of the work. No extra cable to buy. No power wasted because of excessive cable length. No hoisting of heavy cable from floor to floor.

In pipeline operations, the operator moves it as his work progresses —and thus his work progresses *fast!* In plant repairs, the Sureweld Portable is on the job in a jiffy and through with the job — in a jiffy. No shutdown of plant processes in order to tie a motor-generator set into a power line.

In these and many other ways Sureweld Portable increases production and saves its cost over and over again. Its price to you may cause you to revise your budget considerably. Users say they don't know how they got along without this most useful and economical piece of equipment.

Let us demonstrate the Sureweld Portable — any place you say. No cost or obligation. Use the coupon below or contact your nearest NCG branch or NCG authorized dealer.

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J

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☐ Please rush Illustrated Bulletin NH-121 containing full details of the SUREWELD Gas-Driven D-C Arc Welder.

☐ You may arrange, at no obligation to me, a demonstration of the Sureweld.

NAME_____ POSITION_____

FIRM_____

ADDRESS_____

CITY_____ ZONE____ STATE_____

ing which two operators load the two fenders in place as pictured. Simultaneous clamping of all parts then occurs. The operators start the welding cycle and all four heads make their overlapped welds. The heads are electrically interlocked and synchronized to operate without interference. Flux delivery and recovery is fully automatic.

At the conclusion of the weld cycle all heads move to clearance stations. The parts are unclamped and the welded assembly is power conveyed out of the machine while new parts are being inserted.

For further information write to Morton Manufacturing Co., Muskegon Heights, Mich.

Cast-Iron Glass Container Molds

Cast-iron glass container molds that develop chips, cracks or flaws during manufacture or operation can now be easily repaired without preheating by applying a low-temperature welding process developed by Wall Colmonoy Corp., 19345 John R St., Detroit 3, Mich.

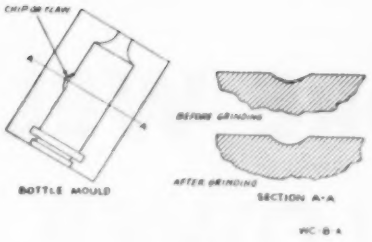


Fig. 1

In this process, the area to be repaired is first ground to remove all oxide and provide a bright base metal surface. Then the area is heated to about 200 F with an oxy-acetylene flame.

A small amount of Colmonoy Ferrus Flux is next applied to the area and heat applied until the flux flows. In the final step, (Fig. 1), Colmonoy No. 20 nickel base alloy in rod form is applied by welding using a reducing flame with a feather about twice as long as the inner cone. After the weld is completed, it is finished by grinding or filing.

The use of minimum heat throughout the entire process avoids fusion of the cast iron and consequent formation of hard spots adjacent to the weld. The Colmonoy No. 20 alloy will not discolor or mark the finished glass (Fig. 2). Since the alloy has a hardness of from 15 to 20 Rockwell C, the welded surface can be easily smoothed by filing.

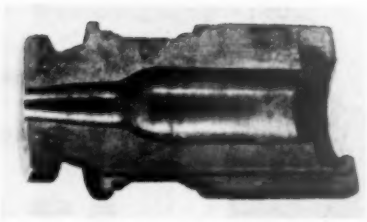


Fig. 2

Colmonoy No. 20 contains chromium, boron, and nickel and has a 2100 F melting point. It has excellent corrosion resistance, very good weldability, fair abrasion resistance and good machinability properties. It is available in $^3/_{16}$ and $^1/_4$-in. diam bare rods.

LIST OF NEW MEMBERS

Effective August 31, 1953

Membership Classification

A—Sustaining Member
B—Member
C—Associate Member
D—Student Member
E—Honorary Member
F—Life Member

ANTHONY WAYNE

Hillbish, Robert William (B)

BIRMINGHAM

Girardeau, J. M. (C)
Harris, George C. (C)
Hodges, T. J. (C)
Murray, W. Frank (C)
Powell, Frank O. (B)
Wilcox, Woodrow W. (C)

BOSTON

Avery, Paul F., Jr. (B)
Hatch, William P. (C)
MacLean, Donald B. (B)
Robinson, Thomas R. (B)

CHATTANOOGA

Angel, Clint (C)
Puryear, John (C)

CHICAGO

Baird, Bruce L. (C)
Marrell, L. J. (B)
Szu, Chung Y. (C)

CLEVELAND

Hoover, Benjiman Oliver (B)
James, Richard, III (B)
Keitz, Henry G. (C)
Kenney, Thomas F. (C)
Mathews, John M. (C)
Morgan, H. K. (B)
Smith, R. E. (B)

COLORADO

McKenzie, David (C)
Reissig, C. E. (B)
Walters, J. A. (B)

COLUMBUS

Fosnaught, C. G. (C)

DALLAS

Hodges, W. D. (B)
Strain, Laroy (B)
Strain, Paul F. (B)

DAYTON

Brown, A. M. (B)

DETROIT

Bellair, John A. (B)
Brandt, E. A. (C)
Bucheski, John (C)
Carswell, James R. (B)
Clifton, Richard J. (B)
Coucke, Henry (B)
De Vreugo, Harold (C)
Janecek, Louis J. (C)
Martin, Henry D. (C)
McIlroy, Hugh M. (B)
Modrich, Jack L. (B)
Patreka, Charles V. (C)
Phelps, Vernon A. (C)
Rogers, Edward L. (B)
Sheenan, J. E. (B)
Stough, Gerald D. (B)
Torrance, Gordon Arthur (B)
Tousignant, Kenneth J. (B)

EASTERN ILLINOIS

Harris, Leonard A. (C)

HARTFORD

Cimmino, Alfred A. (B)
Kirkley, George W., Jr. (C)
Swanson, Everett R. (B)

HOUSTON

Allen, C. B. (B)
Atkinson, Elward F. (C)
Blackwell, J. H. (B)
Bostick, Jack P. (C)
Bredthauer, Raymond O. (C)
Byrd, Jack Reagan (D)

Cason, J. E. (B)
Caswell, John Wesley (C)
Chabert, A. G. (C)
Cheyney, A. (C)
Childs, Henry R. (C)
Cluck, John M. (B)
Conway, Ernest William, Jr. (B)
Dick, Walter C. (C)
Du Bois, John W. (C)
Duke, Dennis L. (C)
Durden, Homer (C)
Edmondson, Alvin Lloyd (C)
Ewing, Josh F., Jr. (C)
Firth, Joe E., Jr. (C)
Firth, Richard M. (B)
Fitze, H. D. (B)
Fontenot, M. M. (C)
Freiley, S. P. (C)
Fry, J. P., Jr. (C)
Goldberg, Gilbert B. (C)
Gould, L. O. (C)
Green, Jay E. (B)
Gustafson, Robert E., Jr. (B)
Hardy, C. A., Jr. (B)
Harton, Gerhart Booth (B)
Haunschild, Otho H. (B)
Hegter, James F. (C)
Hesskew, Mentford (C)
Johnston, Richard M. (B)
Kaup, L. F. (C)
Kentner, Norman L. (C)
Ketchbaw, Jewelle (B)
Land, Paul H. (C)
Landry, Luke D. (C)
Lantz, Ersel G. (B)
Lightfoot, W. E. (C)
Long, Hollis W. (B)
Mai, Edward T. (B)
Manley, W. D. (C)
Morrow, W. K. (C)
Muesse, Leroy A. (C)
Mullis, F. J. (C)
Nevill, J. L. (B)
O'Donnell, D. J. (C)
Perkins, Jack M. (C)
Poole, Grover Q. (B)
Price, Lee H. (B)
Quillen, J. E. (B)
Raver, C. Leo (B)
Redding, Jack T. (C)
Regan, Floyd M. (B)
Rowell, Cecil (C)
Selman, Bartlett (B)
Sprague, Roy D. (C)
Strickland, R. F. (B)
Tanner, R. E. (B)
Thornton, W. J. (B)
Trahan, L. W. (B)
Van Wart, Charles (B)
Vincent, C. R., Jr. (C)
Walker, W. T., (B)
Williams, Paul M. (C)
Wood, C. A. (B)
Young, J. O. (B)

INDIANA

Miller, Carl L. (C)

IOWA-ILLINOIS

Morrow, Claire W. (B)

KANSAS CITY

Lintner, Harry L. (C)
Quantic, Myron W. (B)
Wallow, Robert W. (B)

LEHIGH VALLEY

Pyne, Kenneth C. (C)
Klimek, Michael A. (C)

LONG BEACH

Davison, Lester (C)

LOS ANGELES

Bajari, William R. (C)
Casebeer, A. J. (C)
Dull, V. L. (C)
Grow, Oliver W. (C)
Hannson, M. (C)
Hayes, Richard C. (B)
Jensen, Clarence D. (B)
Karr, Milton (B)
Kavenaugh, J. A. (B)
Large, William R. (B)
McGucken, M. M. (C)
Nance, F. E. (C)
Omori, T. T. (B)
Pfefferkorn, O. T. (C)
Wickham, R. W. (C)

LOUISVILLE

Boosey, L. N. (B)
Martin, J. D. (B)

MAHONING VALLEY

Hudis, Michael (C)
Soman, Robert (B)
Verbanic, John (C)

MARYLAND

Smith, Richard L. (C)

MICHIANA

Torian, T. S. (C)

MILWAUKEE

Graham, John L. (B)
Gruell, Jules R. (B)
Hesse, A. D. (B)
Kloiber, Frank (B)
Kowske, Albert L. (C)
Kroesch, Milton F. (B)
Loebel, Maurice G. (B)

NEW JERSEY

Albans, Thomas A. (B)
Cushman, Maurice E. (B)
Davis, William T. (B)
Helmbrecht, W. M. (C)
Lewis, Carl F. (B)

NEW ORLEANS

Pyne, W. G. (C)

NEW YORK

Bertail, Norman (B)
Bohlen, Richard C. (C)
Brokaw, N. S. (C)
Frissell, Harry M. (B)
Rose, Charles A. (B)
Winter, Gustave A. (C)

NIAGARA FRONTIER

Bantelman, Albert R. (C)
Baumler, Donald F. (C)
Fitzgerald, Lee W. (C)
Kester, Robert L. (C)
Lake, Bainwood B. (C)
Lyall, Richard G. (C)
McCann, James R. (C)
O'Rourke, C. E. (C)
Stoiber, Aloysius J. (C)
Trefts, George M. (B)
Woodward, Robert C. (B)

NORTHEAST TENNESSEE

Clowers, Lawrence H. (B)
Martin, Walter E. (B)
Rogers, Everette N. (B)
Shipwash, Raymond (B)

NORTHWEST

Bjostad, J. C. (B)
Maynard, Marcus Jr., (C)

OKLAHOMA CITY

Folsom, Richard L. (C)

PEORIA

Cain, Francis E. (C)
Leyshon, A. B. (B)

PHILADELPHIA

Althouse, Alfred K., Jr. (B)
Bleil, Raymond G., Jr. (B)
Brown, Edwin Owen (B)
Eckman, Frank H. (B)
Esherick, Peter (C)
Near, Reed A. (C)
Sandler, Frank (B)
Thoman, J. W. (B)
Waite, Robert G. (B)

PITTSBURGH

Morse, Kenneth M. (B)
Murphy, Edward F., Jr. (B)
Nagle, Paul G. (C)
Thom, William F. (C)

PORTLAND

Cutler, Harmon (B)
Deines, Frank V. (B)
Kitchen, Alfred W. (B)
Knab, Harold F. (B)
De Long, Duane (B)
Siegner, George K. (B)
Wyman, Walter L. (C)

PUGET SOUND

Anderson, Raymond C. (B)

RICHMOND

Garbee, W. C. (B)

ROCHESTER

Hartwick, Glenn E. (C)

SAGINAW VALLEY

Sippel, Karl C. (C)

ST. LOUIS

Elgan, Eugene M. (C)
Eyth, William Martin (C)
Gowan, John William (B)
Nooter, John H. (B)

SALT LAKE CITY

Brown, Verle H. (B)
Markosian, A. (B)
Solomon, Wendell J. (B)

SANGAMON VALLEY

Etter, Willis O. (C)
Sullivan, Lee A., Jr. (C)

SUSQUEHANNA VALLEY

Burba, Joseph A. (B)
Kastner, Edward S. (B)

SYRACUSE

Rubury, Roy A. (C)

TULSA

Fogarty, John E. (C)
Nichols, Roland F. (B)

WICHITA

Dill, Edward (B)
Hendrich, Leo J. (C)
Mitchell, Melvin M. (B)

YORK-CENTRAL PENNSYLVANIA

Fitzgerald, John Edward (B)

NOT IN SECTIONS

Colt, Henry F., Jr. (C)
Gallagher, Maurice J. (C)
Gibson, Oscar F. (B)
Gurholt, John Jacob (C)
Hudson, William T. (C)
Jack, William (C)
Kawamura, Fumio (B)
Morrisey, William R. (C)
Santini, Robert (B)
Shimizu, Yasushi (B)
Taylor, Albert F. (C)
Thomsen, Thomas (B)

Members Reclassified

During the month of September

ARIZONA

Tesmer, Arthur W. (C to B)

CLEVELAND

Yarrow, Ross J. (C to B)

DALLAS

Berry, Floyd H. (C to B)

DETROIT

Kendall, Larry T. (C to B)
Smith, William H. (C to B)
Walker, Robert E. (C to B)

HOUSTON

Doyle, J. H. (C to B)
Dwyer, W. H. (C to B)
Hoffman, Joseph H. (C to B)

IOWA-ILLINOIS

Crelly, Jack W. (C to B)

KANSAS CITY

Burrows, Dellmar (C to B)

MILWAUKEE

Martinson, E. O. (C to B)

NEW JERSEY

Anderson, Stanley (C to B)
Cunningham, J. Warden (C to B)

NEW YORK

Vom Steeg, E., Jr. (B to F)

NIAGARA FRONTIER

Stark, Louis E. (C to B)

NORTHWEST

Hanson, A. H. (B to A)

PUGET SOUND

Smith, William R. (D to B)

SAN FRANCISCO

Greene, H. U. (C to B)

NEWS OF THE INDUSTRY

Titanium Research

Dr. Ernest F. Nippes, head of welding research at Rensselaer Polytechnic Institute, Troy, N. Y., and member of the metallurgical engineering faculty, is conducting a program of research for the Detroit Arsenal, of the Army's Ordnance Corps, it has been announced by Dr. Wendell F. Hess, director of Rensselaer Research.

Under the contract of the Arsenal with the Institute Dr. Nippes and associates at Rensselaer are making an engineering study to determine the residual stresses in welded titanium joints.

C. B. Herrick Manufacturing Corp. in Automatic Welding Field

The C. B. Herrick Manufacturing Corp. of Cleveland, Ohio, is building a line of standard equipment for automatic welding to be marketed by dealers throughout the country. They will also feature machinery specially designed and built for automatic welding applications.

Clayton B. Herrick is President of the corporation and has spent many years in the Welding Industry. He has been associated with the Lincoln Electric Co., Industry and Welding Publications, and for the past several years has been identified with the C. B. Herrick Co. of Cleveland, Ohio. Mr. Herrick is a member of the AMERICAN WELDING SOCIETY.

Willard C. Gunzelman is Vice-President of Engineering and Production. Mr. Gunzelman has a wide background of engineering and design in both the electrical and mechanical field. Recently, he has been associated with automatic welding with one of the largest producers of automatic equipment.

Jack J. Jarms is Vice-President of Sales and has been actively engaged in welding engineering for many years with the aircraft and shipbuilding industries. For the past few years he has been engaged as Sales Manager of the C. B. Herrick Co. Mr. Jarms is an AWS member.

"With the current trend in the welding industry, we are firmly convinced that automatic welding will make tremendous strides in the near future. Research and development has proved that automatic welding has not been fully explored, nor exploited by the fabricating companies," Mr. Herrick commented

Baltimore Welding Clinic

A Welding Clinic and Show will be held in Baltimore, Md., on November 19th and 20th from 2 to 9 P.M. It will occupy 5000 sq ft of floor space in the store and warehouse of Earlbeck & Landrum, Inc., 1023 Cathedral St., Baltimore 1, Md.

All of Earlbecke & Landrum's principal suppliers will have factory engineers to demonstrate the newest welding methods and processes. George E. Linnert, Senior Research Engineer of Armco Steel, who recently revised the book *Welding Metallurgy*, will be available to solve any welding or welding metallurgical problems that the invited guests may have.

Meeting of National Welding Supply Association

Norman H. Gimmy, account executive of Beaumont, Heller & Sperling, Inc., Reading, Pa., advertising agency, addressed the National Welding Supply Assn., Eastern Zone, on "The Value of a Hard Hitting Direct Mail Campaign to Welding Supply Distributors" at the organization's annual meeting September 17th to 18th in the Hotel Statler, N. Y.

In his summary, Mr. Gimmy told the group that direct mail can: (1) establish the "where to buy" of nationally advertised products sold by the distributor, (2) sell his service as a distributor, (3) do the job of promotion at low cost if handled properly.

Abstracts of British Welding Patents

The A. F. Davis Welding Library located at the Ohio State University in Columbus, Ohio, has recently acquired over 2200 abstracts of British Welding Patents which are classified in the usual manner according to R. S. Green, Professor and Chairman, Department of Welding Engineering at the University.

This library was established in 1942 by A. F. Davis, Vice-President and Secretary of the Lincoln Electric Co., Cleveland, Ohio. The objective of the library was to assemble in one place all the important literature on welding. It is located at the Ohio State University, the only university offering a five-year course in welding engineering and leading to the degree of Bachelor of Welding Engineering.

The facilities of the library are offered to industrial engineers, designers and technicians and others interested in research and information on the various forms of welding and their application to design, construction, manufacture or maintenance of metal products and structures.

Over 15,000 patents have now been classified in the A. F. Davis Welding Library collection.

Inquiries for patent searches have been received from nine states, the District of Columbia, France and Great Britain, to date.

Farm Boys Receive National Welding Awards

Farm boys in the State of California took 15 of a total of 100 cash awards in The James F. Lincoln Arc Welding Foundation's 1952-53 annual Arc Welding Award Program. This was the best performance by boys in any one state. The Foundation's 1952-53 awards were made to high-school farm boys in 27 different states throughout the country for their descriptions of how arc welding is or could be used in farming.

The First Award was given in Kansas, however, rather than California. Valgene Slingsby of Clay Center, Kan., received $600 for his description of how he made labor-saving equipment for use on the 240-acre diversified farm he works with his father. The equipment included a manure loader, a trailer, lawn mower and clothes-line poles. His teacher, Ray Morrison, whose class produced two other awardees, also benefited from the Award. In honor of Valgene's achievement, the Foundation gave Clay Center Community High School $600 to be used for the improvement of the shop course.

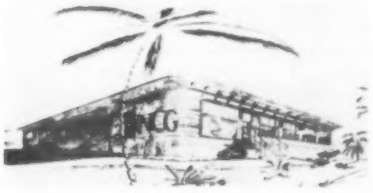
The Second Award of $400 was made to John Dewald of Ritzville, Wash. He described a portable hydraulic hoist that he made to use in his father's farm shop. Ritzville High School also received $400 for use by instructor, James Webb, in improving the shop course.

Donald Rogers of Firebaugh, Calif., and Roger Thieme of Kimberly, Idaho, each received $200 Third Awards. Donald Rogers described a two-cow stock trailer and Roger Thieme, a baled hay elevator. Their schools, Dos Palos Joint Union High School and Kimberly High School, each received $200 for their shop courses.

Awards ranging from $600 to $25 were made for descriptions of a wide variety of home-arc-welded repairs and labor-saving

equipment. Projects described varied from elaborate backsaving devices, such as loaders and trailers, to more simple things such as can racks and yard gates.

The Foundation is sponsoring a similar Award Program for the 1953-54 school year. It is open to all high-school students living on farms or ranches. A Rules booklet telling how to enter and showing pictures of previous award projects is available from The Lincoln Arc Welding Foundation, Cleveland 17, Ohio.



NCG Plant

The accompanying sketch shows an architect's drawing of the National Cylinder Gas Co.'s plant now under construction in Tampa, Fla. The plant, which will produce oxygen and nitrogen, is scheduled for completion this month.

New Welding Supply Firm

Ken Luts, formerly general field manager for All-State Welding Alloys Co., White Plains, N. Y., purchased the welding supply division of Bruce Barton Pump Supply Co., San Jose, Calif. Mr. Luts will operate as the San Jose Welding Supply Co.

The new firm is temporarily located at 940 S. First St., but construction of new quarters at 1000 S. Second St. is under way. The new building will be 50 by 80 ft, with drive-parking facilities and ample showroom space in front.

Mr. Luts began his welding career in the Navy in 1935 and worked at Welders Supply Co., Seattle; Ferrofix Welding & Engineering Co., Seattle; Puget Sound Naval Shipyard, Winslow Marine Shipbuilding Corp. and also as a senior inspector, Ship Construction (welding), Bureau of Ships.

Prior to being appointed general field manager for All-State, Mr. Luts had been regional manager for 11 western states and Hawaii. In the past five years he had conducted 170 clinics and 1600 plant demonstrations.

Ken Luts

National Welding Equipment Co. Expands

National Welding Equipment Co. of California, now in their 43rd year in the welding equipment business, announces the purchase of buildings and parking area adjacent to their present plants. Of the newly acquired 16,000 odd sq ft of buildings, some 1700 sq ft will be used for display; the shop will be enlarged by nearly 10,000 sq ft while the store rooms will get an added 2200 sq ft. The Engineering and Development Departments will be expanded by 2200 sq ft of floor space.

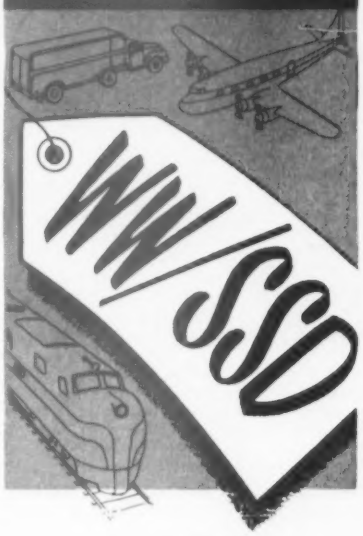
Large Testing Machine Installed

Testing of weldments, large castings, structural members and other equipment will be open to industries in the Montreal area since recent installation of a new universal hydraulic testing machine of 400,000 lb load capacity at McGill University, Montreal, Canada.

Final installation tests were made recently by Baldwin-Lima-Hamilton Corp., Philadelphia 42, Pa. The new Baldwin machine is 22 ft high overall and has 6½ ft clear space between columns. Maximum compression space below the sensitive crosshead is 12 ft and maximum tension space is 11 ft 2 in. without allowance for stroke, which is 10 in. maximum.

McGill University had three reasons for installing the new Baldwin testing machine, according to Prof. V. W. G. Wilson, superintendent, Materials Testing Laboratory, Department of Civil Engineering.

ELECTRODES, HOLDERS, FORGINGS, CASTINGS, ALLOY ROD & BAR STOCK
. . . For Better Resistance Welding.



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Service is what you can count on from Weiger-Weed. Many of the electrodes and holders listed in WW Catalog 14.650-4 are carried in stock. And, by means of a streamlined order-handling system, shipment is made from this stock on the same day the order is received. If there is not sufficient quantity on hand, part of your order is shipped immediately, the remainder later.

Along with this new, fast SSD service, you can continue to count on WW top quality in resistance welding materials.

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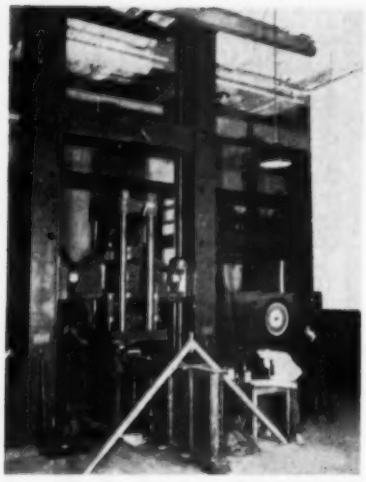
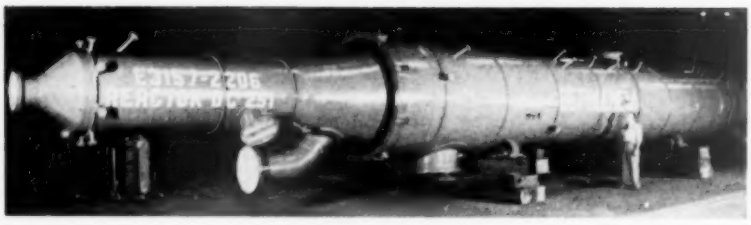


A transverse test of a welded plate girder was among the first projects assigned to the new Baldwin testing machine at McGill University

First, it is a research tool capable of taking large structural members and testing them in their working range. Second, it can be used for undergraduate demonstrations to large classes. Third, it is being made available to industries in the Montreal area for testing structural members, weldments, large castings and other fabricated parts.



Reactor Tower

The accompanying photo shows a reactor tower, important member of the intricate equipment used in the distillation and separation of crude oil into its constituents, ready for shipment from the Bethlehem, Pa., plant of the Bethlehem Steel Co. Made of welded steel plates, the tower weighs 24 tons and is approximately 77 ft long. Its inside diameter ranges from 7½ ft at the large end to about 4 ft at the small end.

Aluminum on the Sky Line

America's first aluminum skyscraper—the 410-ft Alcoa Building in the heart of Pittsburgh's Golden Triangle—was dedicated in September, marking completion of the lightest building for its size ever built. Twenty-five floors of the ultra-modern, 30-story structure are now occupied by Aluminum Company of America; the balance has been rented to tenants.

Begun in May 1950, the Alcoa Building can rightly claim more building innovations than any other structure of modern times. Its exterior walls are sheathed with hundreds of 6- by 12-ft aluminum panels. Its aluminum windows—roughly 4 ft square—are reversed for cleaning from inside the building; weather is kept out by means of an inflatable rubber tube recessed in the window's aluminum frame. Completely air-conditioned, the building's offices are heated and cooled from aluminum ceilings. All electrical wiring and most of the plumbing is aluminum, as are lighting fixtures, elevator cabs, partition framing and many thousands of pounds of interior trim.

The Alcoa Building's 4½-story glass and aluminum entrance—separated from the sheer, 410 ft tower—is built from the top down. Framing members are suspended from two huge cantilever beams which jut out at the fifth floor level. A rooftop aluminum cooling tower—largest of its type ever constructed—cools conductor water for refrigeration equipment which air conditions the building. Building designers have used several types of marble to complement major uses of aluminum inside the building. Executive offices, rooftop boardroom suite, reception foyers on most floors and many administrative offices have been custom furnished in the most modern contemporary sky line.

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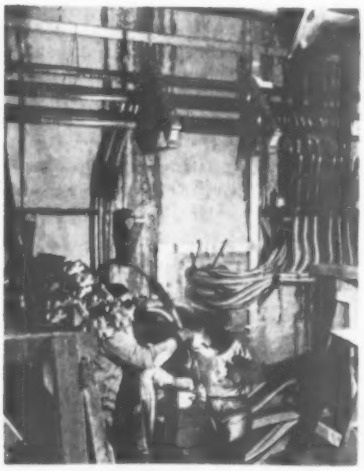
Dramatic photograph shows workman welding two sections of aluminum pipe by the heliarc method



Field welding 750,000 CM aluminum cable limiter to; tee connector for connection to 3,000,000 CM aluminum ring bus

Designed by Harrison & Abramovitz, New York, the Alcoa Building was constructed by the George A. Fuller Co., New York and Pittsburgh. Two Pittsburgh architectural firms—Mitchell & Ritchey and Altenhof & Bown—assisted in its design.

Michigan Pipe-Line Project

Men and machines were working day and night in Northern Michigan last summer to beat the early winter deadline set for the completion of one of the longest and largest crude oil pipe lines ever to be constructed.

Early in August the 30-in. pipe, which was being laid between Sarnia, Ontario, and Superior, Wis., for the Lakehead Pipeline Co., was being inched through both the lower and upper peninsulas of Michigan to the Straits of Mackinac. There it was to divide into two 20-in. sections to make one of the longest, deepest and most spectacular water crossings ever recorded in pipe-line history.

Moving across the Straits of Mackinac at the same time were three barges engaged in towing a 4-mile length of 2-in. cable which was to be attached to the pipe. On the south shore a giant Diesel-powered winch was waiting for the connection to be made so the first of eight 2500-ft sections of pipe could be started out across the Straits. Crew boats, utility craft and barge tenders were churning up the waves and loud speakers barked orders from every side.

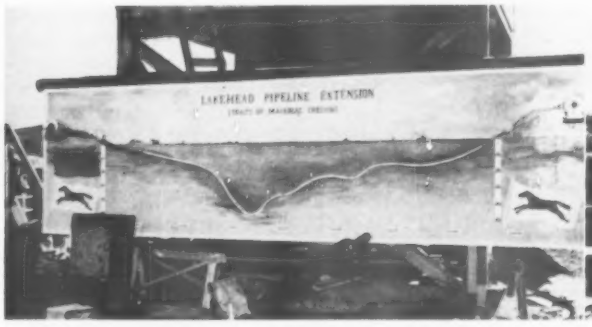
On shore fleets of Diesel-powered welding machines, ditchers, tractors, power shovels and other equipment were working inland a hundred miles or more. The sharp reports of dynamite charges opening up rocky terrain on the line's right-of-way resounded over the country-side. Workmen were sloshing around in Upper Michigan swamplands and here and there a piece of equipment would bog down to the axles in the wet, soft terrain. But the line was going through.

The Straits crossing was in the hands of the Merritt-Chapman and Scott Corp. of Cleveland, Ohio, with Midwestern Constructors, Inc., of Tulsa, Okla., working as subcontractors. Midwestern also had sections of the line under construction both above and below the Straits.

The accompanying illustrations show some of the equipment being used and some of the jobs being done as the big

Welders working near Engadine, Michigan use wooden platforms to keep from sinking knee deep into water-soaked terrain

pipe was being readied and pulled across the Straits and then pushed westward to Superior, Wis. There it was to be connected with the Interprovincial pipe line of Canada and then the two lines, covering a distance totaling 1765 miles, were to comprise one of the longest and highest capacity pipe lines in the world.



"Sidewalk" engineers watching the animated billboard from a specially constructed grandstand, were currently kept posted on the pipe's underwater progress

The first section of the pipe has been started out across the Straits. Buoys ride the pipe at regular intervals to keep it from sinking immediately to the bottom

abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 25, D. C.

2,650,162—Weld Rod—Arthur T. Cape, Los Angeles, Calif., assignor to Coast Metals, Inc., Little Ferry, N. J., a corporation of Delaware.

This novel weld rod disclosed by this patent is especially adapted for facing parts of apparatus used in copper metallurgy and subjected to corrosive effects of molten copper, copper oxide and the like. The rod contains chromium in amounts of from about 24 to 40%, copper in amounts of from about 0.5 to about 3%, and carbon from 0.5 to 1.75%. The balance is primarily iron.

2,650,420—Method and Apparatus for Brazing Aluminum Fins to Steel Tubes—Charles H. True, Chicago, Ill., assignor to Combustion Engineering, Inc., a corporation of Delaware.

Mr. True's patent relates to apparatus and method of the character indicated. The method comprises applying a brazing flux to the surfaces to be bonded, mounting the fins on the tube and applying brazing material, and surrounding the portion of the tube carrying the fins with a gaseous atmosphere. The gaseous atmosphere is heated to a high temperature approaching the brazing temperature while simultaneously a fluid heated to the brazing temperature is circulated through the interior of the tube.

2,651,023—Variable Transformer for Alternating Current Welders—Francis B. Miller, Dayton, Ohio, assignor to The Commonwealth Engineering Co. of Dayton, Ohio, a corporation of Ohio.

Mr. Miller's patent is on an autotransformer for welding action and it includes a coil having primary and secondary windings. The coil has opposite ends forming output terminals with the primary winding being connected to a primary supply circuit and being variably coupled with the secondary winding. The secondary winding has permanently coupled, in substantially maximum inductive relation therewith, a short-circuited compensating winding.

2,651,701—Portable Spot Welder—Arthur Oliver Backen, Chicago, Ill.

This patent relates to a portable, manually operable spot welder including a transformer with primary and secondary windings. Frame means are provided and a pair of welding jaws are connected for energization to the secondary winding and are carried by the frame means, being mounted for movement toward and away from each other. An operating lever is pivoted on the frame means for controlling the position of the welding jaws. Other special control means are associated with the operating lever for controlling the supply of alternating current to the primary winding in correlation with the operative positioning of the welding jaws.

2,652,243—Vertical Weld L Cutting and Beveling Machine—Wade R. Reed, Tulsa, Okla.

In this machine of the type indicated, specialized positioning means are provided for a tube, and a cutting torch is carried in the machine for operatively engaging the tube positioned in the apparatus.

2,652,472—Welding Electrode Holder—Ernest W. Huebner, Detroit, Mich., assignor to Weiger Weed & Co., a corporation of Michigan.

This welding electrode holder has a knockout tube positioned within the holder tube and a knockout plunger is provided to control the action of the knockout tube. A water-receiving tube is positioned within the knockout tube and is sealed with relation thereto.

2,652,473—Electric Butt Welding—Carroll G. Gordon, Pasadena, Calif., and Robert R. Cameron, Denver, Colo.

A method of welding the abutting extremities of two metal bars together is disclosed in the patent and includes the steps of connecting the abutting extremities to both bars to one pole of an electric circuit. A relatively thin metal insert is placed between the abutted extremities and connected to the other pole of the electric circuit. An electric current is passed through the circuit until the insert fuses and breaks the circuit.

2,652,474—Method of Heating Opposed Edges of Elongated Members—Phillips Nielsen Sorensen, Cleveland, Ohio, assignor to The Ohio Crankshaft Co., Cleveland, Ohio, a corporation of Ohio.

Mr. Sorensen's patent is on a pressure welding method for generally thin metallic material. Edges of a pair of elongated metallic members are placed in closely spaced parallel relationship and a high-frequency inductor having a main conductor in close-spaced parallel relationship to the edges is provided, and the edges are progressively moved relative to the inductor to induce high-frequency currents to flow in the material. The current through the thickness of the material at any one point is always in the same direction at any one instance and the currents immediately adjacent the edges are very highly concentrated whereas the return currents remote from the edges are relatively unconcentrated. This progressively and rapidly heats the edges to the pressure-welding temperature and, after the edges are so heated, the edges are progressively brought into pressure-welding engagement.

2,653,057—Welded Pressed Steel Dual Wheel—Donald W. Sherman, Milwaukee, Wis., assignor to A. O. Smith Corp., Milwaukee, Wis., a corporation of New York.

Mr. Sherman's patent is on a welded, pressed dual wheel assembly for trucks and the like. The wheel is made from a pair of pressed metal disks having inwardly directed flanges formed on each disk with the flange of one disk being butt welded to the flange of the other disk to provide a hub section on the wheel assembly. A plurality of complementary pressed metal members are welded to the outer periphery of the disks and extend along the periphery of the disks to close the outer circumference of the wheel and provide a bearing surface for the rims accurately centered with respect to the center of the wheel without any machining operation.

2,653,277—Welding Apparatus and Welding System—Ian Jack Pickering, Birmingham, England, assignor to The General Electric Co. Ltd., London, England.

This patent is on a two-phase welding apparatus in which the electrodes are used simultaneously and in close proximity to each other. Electrodes and the work are arranged for connection to the two phases and a common return lead, respectively, of the two-phase welding current supply. Welding current is provided by a Scott-connected transformer for providing two-phase welding current from a three-phase supply. Means responsive to the current flowing in the common return lead are provided to reduce the voltage between the electrodes to a value at which the arc is extinguished by disconnecting one of the three-phase lines from the Scott-connected transformer.

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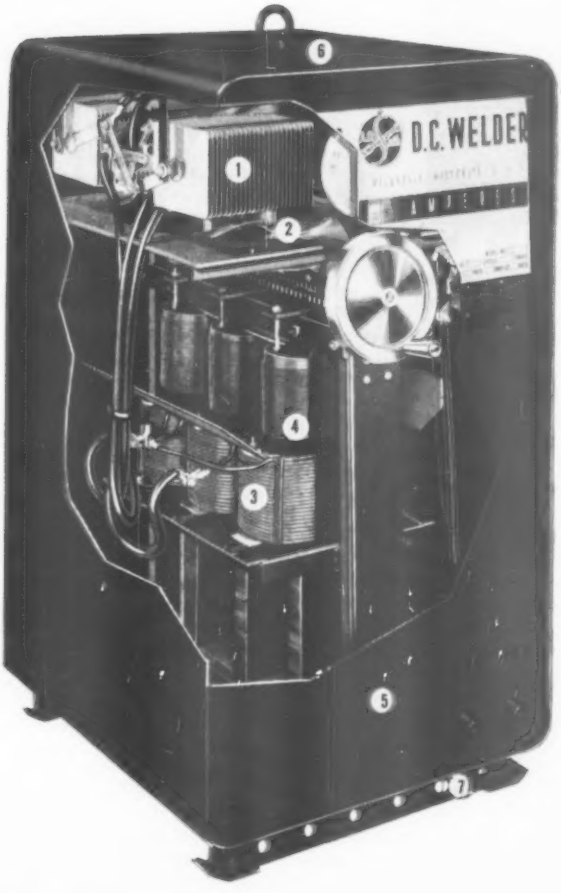

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NEW LITERATURE

Victor Equipment Catalog

Victor Equipment Co., 844 Folsom St., San Francisco 7, Calif., announces their new 4-color 64-page catalog covering their complete line of gas welding and cutting equipment. This catalog is completely illustrated with 4-color illustrations, black and white pictures and complete specification charts for full information and ease of ordering.

Write Victor today for your free copy of Catalog 20.

Radiography in Industry

Technical Operations, Inc., 6 Schouler Court, Arlington 74, Mass., has issued a new catalog entitled "Equipment and Sources for Gamma Radiography in Industry." Copies of this catalog are available upon request.

Morton Machines

Morton Manufacturing Co., Muskegon Heights, Mich., announce four new bulletins on their welding machines as follows:

Bulletin 39 covering a Morton Tank Installation.
Bulletin 43 covering Morton Girth Welding Machines.
Bulletin 44 Covering Morton Stake Welding Machines.
Bulletin 45 covering Morton Special Fixtures.

Fast, Low-Cost Hard Surfacing

A new bulletin describes many applications of this new, fast, low-cost process of applying hard surfaces.

Guide rolls in a steel rolling mill are typical. Here is their experience with this new process. This rolling mill had tried various methods to improve the service life of guide rolls used in mill roller guide boxes.

Another application is that of agitator shafts in processing equipment in a large chemical plant. The shafts are from 2 in. diam, to $3^{7}/_{16}$ in. diam, and from 12 to 17 ft long. Two sections of each shaft run in stuffing boxes. These areas are Spraywelded to a finished depth of 0.025 in. on the radius. Improvement in service life over non-Spraywelded parts has been so impressive that this plant will not put a new shaft in service unless it has been Spraywelded on these two areas.

Other applications include acid sludge pump rods, deep well pump rods, guide wheels, glass mold plungers, thermocouple wells and many others. For further information write for Bulletin 53B to Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y.

Metallized Stainless Steel

A leading manufacturer of Diesel and gas engines and compressors, in business over 100 years, is quite up to date in its ideas and processes.

As a result of their experience with the repair of piston rods by metallizing, all new piston rods to be used for corrosive gases are now made of carbon steel and metallized with stainless in the process of manufacture.

Several sizes of compressors are made in 2-, 3- and 5-cylinder models from 12-in. bore by 14-in. stroke to 30-in. bore by 48-in. stroke. The rod shown in the picture is 4 ft 8 in. long, 3 in. in diam for a 4-ft 2-in. length, with a 4-in. flange at that point on the shaft, and the remaining length having a diameter of 2.494 in. Procedure is to undercut 42 in. of the 3-in. diam portion of the shaft 0.030 in. undersize on the radius, thread this an additional 0.025 in. deep 14 per inch. Threads are then prepared with a special shaft preparing tool and the surface metallized with stainless, allowing 0.025 in. for finish grinding.

This metallizing report is just one of many appearing in the current issue of the *Metco News*. Other applications include the salvaging of press rolls where close dimensional tolerances are required; repair of bell-ends, electric motor collector rings; and protection of playground equipment against rust.

You can get a copy of *Metco News*, Vol. 6, No. 10 by writing to Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y.

Review of ASTM Research

Reprinted copies of the "Review of ASTM Research" as published in the December 1952 and January and February 1953 ASTM Bulletins are now available.

This material, prepared by the ASTM Administrative Committee on Research, summarizes the work of the various technical committees of the Society as of May 1953.

Copies of this 22-page pamphlet are available, without charge, at ASTM Headquarters, 1916 Race St., Philadelphia 3, Pa.

Metals at Elevated Temperatures

The papers and discussions in this Symposium, sponsored by the Joint ASTM-ASME Committee on Effect of Temperature on the Properties of Metals, were presented at the 1952 Annual Meeting of the American Society for Testing Materials held in New York City.

The material presented in this Symposium concerns the effects of notches on metals at elevated temperatures, under either static or dynamic loading, and discusses results of current research on the subject. It also deals with another aspect of the behavior of metals at elevated temperatures, namely, the metallurgical changes which occur during heating at elevated temperatures, and the effects of these on strength and ductility.

Copies of this 256-page Symposium, heavy paper cover, can be obtained from Headquarters of the American Society for Testing Materials, 1916 Race St., Philadelphia 3, Pa., at $3.25 each.

Electric Arc Welding

Electric Arc Welding is the title of a new book of 280 pages for $4.00 published by the American Technical Society, 848 E. 58th St., Chicago 37, Ill. The book was written by John B. Austin, Welding Engineer, Republic Structural Iron Works, Cleveland, and former chairman of the Cleveland Section of the American Welding Society, who has contributed an important instrument of instruction to be placed in the hands of those whose ambitions are to achieve proficiency in the welding processes.

Starting with a thorough groundwork of fundamentals, the author explains welding equipment and its use. He

then applies these fundamentals to the major fields of welding: machinery, structural, pipe and hard surfacing. Automatic as well as manual welding, are covered in detail, along with the three principal shielding processes employed in automatic welding: hidden, inert arc and the atomic-hydrogen process.

Employment Service Bulletin

Positions Vacant

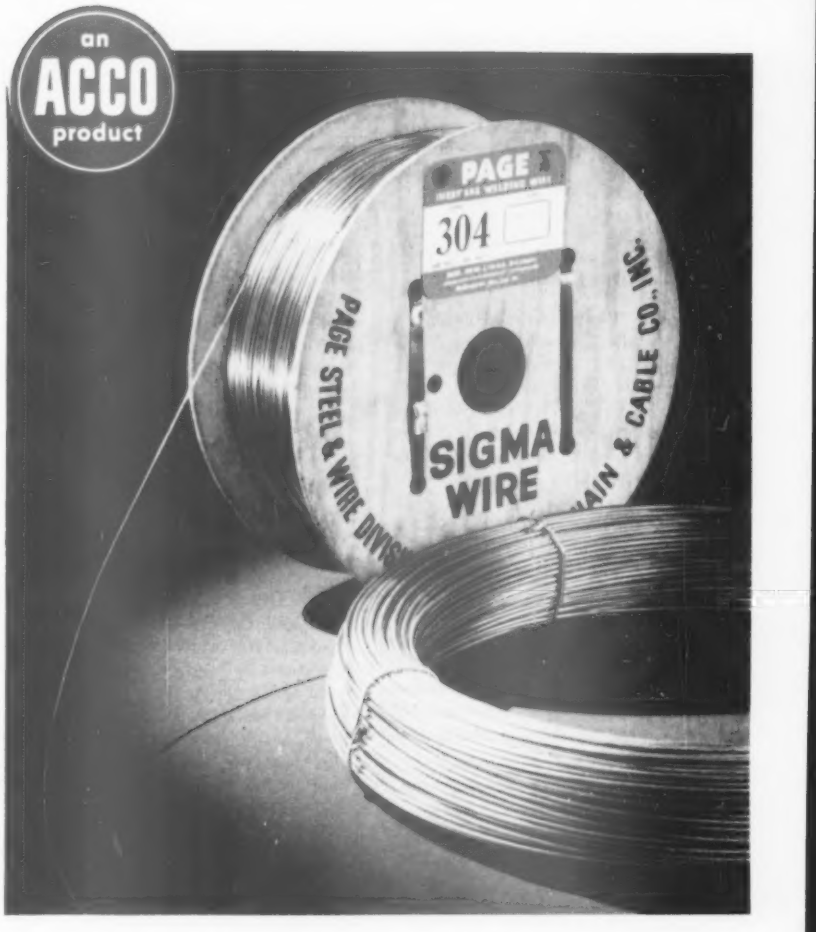
V-301. Welding Engineer for Research Laboratory. Applicant should have theoretical and practical knowledge of welding, including nonferrous alloys; complete knowledge of inert gas-shielded arc-welding methods and equipment; experience in training and qualifying welders. Some traveling necessary. State experience, education, present salary and military status in first letter. All replies confidential.

V-302. Welding Research Engineer. Engineer or metallurgist with experience or training in welding research needed for laboratory study of welding problems in the construction, maintenance and repair of process industry equipment. Major user of metal products in the Chicago area. Prefer man with advanced degree, age 25 to 35. Salary dependent upon training and experience.

Services Available

A-641. Graduate Engineer with degree in Welding Engineering, desires to join organization doing research or development connected with welding processes. Age 28. Two years' experience in preparation of specifications for welding process applications, developing welding procedures and assisting in welding design. Previous experience as welding operator. Prefer Midwest location.

A-642. Welding Engineer. Bachelor of Metallurgy. Age 28. Single. Experienced in the fabrication of heavy welded products, code pressure vessels, using plain carbon, alloy steels and nonferrous metals. Also experienced in welding cost control, training of welders, field construction and repair, and the college teaching of metallurgy and welding. Good administrator with ability to get results in terms of quality and production. Desire a supervisory position in welding engineering, field construction, production or sales with a progressive organization.



Why You Should Use... PAGE Automatic Welding Wire

• PAGE Automatic Welding Wire is made to rigid specifications which provide consistent characteristics for smooth flow of metal during continuous or intermittent welding. There are many analyses for a wide variety of uses.

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PAGE Stainless Steel Wire is precision thread-wound on 25-lb. non-returnable convenient reels which fit popular arc welding machines. Six Page-Allegheny grades; wire diameters: .035", .045", .0625".

for SUBMERGED ARC Welding

PAGE stainless, low alloy, and carbon steel wire in layer-wound coils, in 22" or 24" mill coils, or on 200-lb. returnable steel reels. Wire diameters from 1/32" to 5/16"; copper coated if desired.

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PAGE Welding Electrodes AND Rods

SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Welding Metallurgy

Bridgeport, Conn.—Fundamentals of Welding Metallurgy was the subject of a good talk given by Richard K. Lee, AWS, Vice-President of Alloy Rods Co., York, Pa., at the September 17th meeting of the *Bridgeport* Section. Slides and motion pictures were used to illustrate the subject.

Coffee speaker was Sgt. Louis D. Marchese of the Connecticut State Police, who told some interesting police anecdotes.

The dinner and meeting were held at Rapps Restaurant, Shelton.

Chicago Officers

Chicago, Ill.—The following officers and committee chairmen have been appointed and elected by the *Chicago* Section for 1953–54:

Chairman—Thomas H. Swisher, Hotpoint, Inc.
First Vice-Chairman—A. R. Meyer, Graver Tank & Mfg. Co., East Chicago, Ind.
Second Vice-Chairman—J. C. Savage, 5700 S. Wolcott Ave.
Sec.-Treasurer—L. C. Monroe, *Welders Digest*, La Porte, Ind.
Chairman, Membership Committee—J. C. Savage
Chairman, Program Committee—A. R. Meyer
Technical Representative—T. B. Jefferson, *The Welding Engineer*

Elected Officers

Decatur-Springfield, Ill.—The *Sangamon Valley* Section announces the following elections and appointments:

Chairman—E. Donald Peters, Leader Iron Works, Inc., Decatur, Ill.
First Vice-Chairman—Lester L. Baugh, Springfield, Ill.
Secretary—LeRoy H. Smith, Decatur, Ill.
Treasurer—Paul E. Mallow, Springfield, Ill.
Chairman, Membership Committee—Carl H. Shaw, See-Mo Welding Products Co., Springfield, Ill.
Chairman, Program Committee—Jerome P. Hertel, Decatur, Ill.

Plant Visits

Denver, Colo.—The *Colorado* Section received a cordial invitation for a visit to the plant of the Gardner Denver Co., manufacturer of rock drills, compressors and other air-operated equipment. Members of the Section visited the plant on August 21st when practically all departments were in operation, including the machine shop, the welding shop, heat treating and assembly line. In addition there were some mine car loaders and rock drills in actual operation. A very pleasant meeting was concluded with refreshments served by the host.

Another extremely interesting plant visit was made by the Colorado Section on September 8th, this time to the United Air Lines new building. This is the operating headquarters, which covers their flight operations for the entire system. In this building is the payload control center where all flight reservations are controlled for the entire system, handling some 16,000 phone calls per day.

Allis-Chalmers Visit

Springfield, Ill.—An exceptionally entertaining talk on "The History of Allis-Chalmers, Springfield, Ill., Works" was given by Henry J. Detjen of that company at the September 15th dinner meeting of the *Sangamon Valley* Section held at the Allis-Chalmers plant. An impressive tour of Plant No. 1 was made following Mr. Detjen's talk. Both added up to an evening well spent.

Electrode Coatings

Detroit, Mich.—The speaker at the September 11th meeting of the *Detroit* Section, Harry F. Reid, Jr., AWS, The McKay Co., did an outstanding job in presenting a complicated subject in an understandable manner.

The "mysteries" of electrode coatings were revealed and explained by Mr. Reid. His talk, illustrated with slides, described the manufacturing processes and characteristics of the various types of coatings and explained their application.

The importance of the subject was proved by the many questions which the audience asked and the discussion which followed.

Another fine talk was given by the coffee speaker, Nicholas George of the Murray Corporation of America, whose subject was "A Touch of Recognition."

Election of Officers

Detroit, Mich.—The *Detroit* Section announces the election and appointment of the following officers and chairmen of committees for the 1953–54 season:

Chairman—J. M. Johnston, Murray Corporation of America
First Vice-Chairman—Roy L. Clark
Second Vice-Chairman—J. E. Rainey
Secretary-Treasurer—J. R. Stitt, The R. C. Mahon Co., P. O. Box 4666, Detroit 34
Chairman, Membership Committee—Walter Kroy, Ford Motor Co.
Chairman, Program Committee—Roy L. Clark
Chairman, Program Announcements Committee—Harold Leininger
Technical Representative—L. E. Wagner, 7830 Reuter, Dearborn

Submerged Arc Welding

Des Moines, Iowa—The *Iowa* Section held its first fall meeting on September 17th at the New Pastime Restaurant, Des Moines. Forty-one members and guests enjoyed another fine smörgåsbord style dinner. The ensuing speech was presented by J. A. Smithers, AWS, District Manager of the Lincoln Electric Co. at Moline, Ill.

After a brief introduction by First Vice-Chairman George Sullivan, the audience was presented with Mr. Smithers' speech on "Submerged Arc Welding." The pros and cons for the use of this process, the advantages to be gained, such as superior quality welds, faster welding speeds, less distortion, versatility in the semiautomatic application, were all discussed in an informal fashion which held the audience's interest. In addition, Mr. Smithers showed, by blackboard illustrations, the cost factors, joint design considerations and penetration problems supporting the submerged arc process.

Alloy Steels

Erie, Pa.—A metallurgical approach to fabrication problems was covered, at the September 22nd meeting of the *Northwestern Pennsylvania* Section, by Robert W. Bennett, AWS, Chief Metallurgical Engineer, American Locomotive Co., Dunkirk, N. Y., in his talk entitled "Welding of Alloy Steels for Low-Temperature Service." The translation of the data and methods developed in the laboratory to the actual fabrication of pressure vessels and heat exchangers were a major part of Mr. Bennett's presentation.

Mr. Bennett was formerly connected with the Welding Research Division of the Battelle Memorial Institute.

After-meeting feature was the showing of two motion pictures, one, "Going Places" was shown through the courtesy of the General Electric Co., and the other, "Hunting" was shown through the courtesy of the Geiss Film Library.

Newly Elected Officers

Hartford, Conn.—The *Hartford* Section announces the election and appointment of the following officers and committee chairmen:

Chairman—Stephen A. Zane, Manchester, Conn.
First Vice-Chairman—Thomas W. Brady, Westinghouse Electric Corp.
Secretary—Alfred E. Dohna, East Haven
Treasurer—Joseph St. Cyr, Acme Tank & Welding Division, United Tool & Die Co.
Chairman, Program Committee—Morton L. Kenyon, Charles Pfizer & Co., Inc.
Technical Representative—John W. Mortimer, 82 Plymouth Lane, Manchester, Conn.

Ultrasonic Testing

Houston, Tex.—The practical work done to date on inspection of welds with sonic instruments was covered in an illustrated discussion by Dwight J. Evans of the Engineering Test Service, Tulsa, Okla., as the feature of the September 23rd meeting of the *Houston* Section held at the Ben Milan Hotel. One hundred and thirty-one members and guests attended the meeting and 124 were present at dinner.

Mr. Evans established Engineering Test Service, whose activities mainly consist of ultrasonic testing with the Branson Audigage, the Sperry Reflectoscope, McPhar Soniscope, and experimental stress analysis, in 1950.

Before the meeting took place an excellent motion picture film, entitled "Highlights of the Southwest Conference—1952" was shown through the courtesy of the Humble Oil & Refining Co.

Aluminum Alloy Aircraft Spot Welding

The Aircraft and Rocketry Welding Panel of the *Los Angeles* Section started its fiscal year with an excellent program designed to improve resistance welding by having one of their West Coast top-notch welding engineers, C. B. Smith, AWS, relinquish his "Secrets to Success" as a welding engineer, at the September 3rd Panel Dinner Meeting.

Mr. Smith has the unenviable position of being responsible for the fusion and resistance welding procedures, welder and operator certifications, and allied processes for manufacturing the huge C-124 Globemaster airplane which Douglas Aircraft is currently building.

In his lecture, accompanied by picturesque slides, Mr. Smith described the trend of changeover from riveting to spot welding for joining 75S-T aluminum alloys. This is the dream of every welder or Welding Engineer in the aircraft field—to see a plane manufactured by fusion and resistance welding on a high productive scale.

The complexities of such operations need high statistical control and in his lecture Mr. Smith described and thoroughly explained proven methods for such controlling.

A very precise list of "Basic Items on Operator Tests" was discussed and provided the excellent information required for process control of spot and seam welding certification of "B" and "A" class operators.

Piping Industry

Louisville, Ky.—At the first meeting of the new season of the *Louisville* Section held on September 22nd at Korfage's Tavern, Dan Cheyney of Tube Turns, Inc., Louisville, addressed a large attendance on "The Piping Industry—A Challenge to the Welding Profession." In his discussion Mr. Cheyney used slides to illustrate various applications which were a definite challenge to the welding industry. A film depicting the manufacture of seamless tubing was shown and thoroughly appreciated.

Plant Visit

Newark, N. J.—Sixty members and guests of the *New Jersey* Section thoroughly enjoyed a visit to the plant of the Annheuser-Busch Brewery on September 15th, where the equipment was inspected and the product sampled.

New Jersey Officers

Newark, N. J.—The following officers and chairmen of committees have been elected and appointed by the *New Jersey* Section for the fiscal year.

Chairman—N. F. Kiernan, Metal & Thermit Corp.
First Vice-Chairman—Kenneth Koopman, Linde Air Products Co.
Secretary—R. L. Deily, Bound Brook, N. J.
Treasurer—Ford Pollock, Taylor Forge & Pipe Co.
Chairman, Membership Committee—T. McElrath, Linde Air Products Co.
Chairman, Program Committee—E. DiLiberti, Secaucas, N. J.
Technical Representative—N. F. Kiernan

Simplified Metallurgy

New York, N. Y.—Anton L. Schaeffler, AWS, Welding Research Metallurgist, Allis-Chalmers Manufacturing Co., Milwaukee, Wis., spoke on the subject, "Simplified Metallurgy for Welders and Welding Supervisors" before an audience of *New York* Section members and guests on September 15th. The program was preceded by dinner at Schwartz's Restaurant, 54 Broad St., where the Section meetings are held regularly.

The following new members were dinner guests of the Section: E. Leszak, A. J. Panish, D. Whitelaw, A. N. Sherwood, J. M. Leahy, A. A. Strickland, P. B. Cole, J. Leiman and J. T. Helfgott.

Ladies Night

Olean, N. Y.—Ladies Night was held for the June meeting of the *Olean-Bradford* Section. The meeting was held at the Penn Hills Country Club at Bradford, Pa., where a fine dinner was served to 54 members and guests. The program was mostly a get acquainted affair and was most successful. The business portion of the meeting consisted of the election of the following officers for the coming year:

Chairman—M. T. O'Donnell
First Vice-Chairman—Geo. T. McMullen
Secretary—Charles S. Smith
Treasurer—William Payne

A rising vote of thanks was given on motion to the officers who had carried on so well during the past year.

Schedule of Meetings

Peoria, Ill.—Tentative program for the *Peoria* Section for 1953–54 is as follows:

Nov. 18, 1953—Speaker: O. Barnett, A. O. Smith Corp.; Subject: Electrode Metallurgy
Dec. 16, 1953—Subject: Sigma Welding
Jan. 20, 1954—Open for the present
Feb. 17, 1954—Speaker: J. Heuschkel, Westinghouse; Subject: Cracking in Welding
Mar. 17, 1954—Speaker: Roy W. Poe, Tweco Products, Subject: Importance of Welding Circuit Efficiency
Apr. 21, 1954—Subject: Semi- and Full Automatic Welding
May 19, 1954—Annual Business Meeting

Coronation Pictures

Peoria, Ill.—Excellent pictures and commentaries by William Gauss of Gauss Mortuary, of his visit to England during the Coronation were of considerable interest to all attending the September 16th dinner meeting of the *Peoria* Section held at The Sazarac.

Welding Metallurgy

Philadelphia, Pa.—"Metallurgy in Welding" was the subject of an extemporaneous talk given by Dr. Robert H. Aborn, [AWS], of the U. S. Steel Corp., at the September 21st dinner meeting of the *Philadelphia* Section held at Drexel Institute of Technology.

H. Russell Bintzer of the Drexel Staff was the Coffee Speaker. Mr. Bintzer showed films and spoke on the subject "Drexel Geared to Industry."

Rockets

Phoenix, Ariz.—The September meeting of the *Arizona* Section was held on the 16th at the Hotel Westward Ho with approximately 50 members and guests present.

Following dinner, the speaker of the evening, Francis Stevenson, [AWS], of Aerojet-General Corp., Azusa, Calif., spoke on "Welding—Its Application to Rockets." A sound film pertaining to the Aerobee Sounding Rocket, entitled "The Sky Is No Limit" was shown.

Mr. Stevenson is well qualified to speak on the subject of rockets. He is Chairman of the Aircraft and Rocketry Panel of the Los Angeles Section and his position with Aerojet-General also demands that he be well versed in the field. All present agreed that the meeting was very successful and the Section is indebted to Mr. Stevenson and his company for a very informative and highly interesting evening.

Welding Machines

Portland, Ore.—The first meeting of the season for the *Portland* Section was held on September 8th at the Heathman Hotel in Portland. A social gathering preceding the dinner brought new and old members and their guests together.

The speaker of the evening, Gary Willicke, Miller Electric Mfg. Co., Appleton, Wis., talked on a timely subject, "Selenium Rectifiers in Welding." His talk was of an extemporaneous nature coupled with several drawings and illustrations on the blackboard.

Nuclear Energy

Saginaw, Mich.—The first meeting of the new season of the *Saginaw Valley* Section was held on September 11th at the High Life Inn in Saginaw, with a turnout of 73 members and guests. A family style dinner preceded the meeting and everybody enjoyed it.

Speaker at the meeting was E. L. Nugent, Assistant Supervisor of Engineering at the Detroit Edison Co. Prior to his address, Mr. Nugent showed a movie on "'A' Is for Atom" in order to familiarize the audience with the basic concepts of nuclear fission. The movie illustrated the structure of atoms and how they were able to split them.

Mr. Nugent's address told of the work that was being done on the nuclear power project of which he has a part. The slides shown in connection with the talk illustrated some very interesting points about nuclear energy.

Resistance Welding

Sheffield, Ala.—William Farrell, [AWS], Sciaky Bros., Chicago, gave an excellent discussion on "Resistance Welding Fundamentals" at the September 24th meeting of the *Tri-Cities* Section held in the TVA Auditorium.

A film "Design of Arc-Welded Structures" was shown through the courtesy of the Lincoln Electric Co.

Welding Controls

Syracuse, N. Y.—The first meeting of the new season of the *Syracuse* Section was held on Setepmber 9th at the Hotel Onondaga. Coffee speaker was Francis Feeney, Chief of Plant Protection at Carrier Corp., who spoke on the recent fires at Eastman Kodak and General Motors caused by careless welding practices.

Technical speaker for the evening was Stuart Rockafellow, [AWS], Chief Engineer of the Robotron Corp., who spoke on "Electrical Ciruits for Welding Control."

Technical high lights of Mr. Rockafellow's talk included the fact that timed machine functions can be controlled by several means such as a synchronous motor to drive a cam, a variable air orifice or grid control of electronic tubes. Each method has its limitations in application. The vacuum tube type of timer has easily adjustable timing but is usually subject to line voltage variations. The thyratron or gas-filled tube will hold its timing to small variations even with large supply voltage changes. The advantage of the electronic timers is long service life. The thyratron tube can handle high current through millions of operations without the deterioration often encountered with mechanical contacts. Many more facts were brought out in Mr. Rockafellow's talk. In general the talk was in lay terms that were easy to understand and it did much to remove the mystery of the operation of electronic controls.

Syracuse Officers

Syracuse, N. Y.—The following officers and committee chairmen have been elected and appointed by the *Syracuse* Section.

Chairman—James Solar, Greyhound Lines
Vice-Chairman—F. R. House, Westinghouse Electric Corp.
Secretary—Arthur Cottet, Cottet's Welding Supply Co.
Treasurer—Robert Larkin, Smith & Caffrey
Directors—H. E. Miller, Hector McNeil, M. Ames, Kenneth Clark, R. J. Greer, Wayne Tucker, Claude Smith, Edward Fisher, Arnold Fitzgerald, Ed Strand, Harry Wolfe, L. H. Hawthorne, Ben Haskell, Carl Monk, Bidwell Drew and S. H. Monson
Committee Chairmen
Program—Ed Strand
Finance—F. R. House
Membership—F. R. House
Technical—Ed Strand
Hospitality—S. H. Monson
Industrial—H. E. Miller
Constitution—M. Ames

Wichita Officers

Wichita, Kan.—The *Wichita* Section announces the election and appointment of the following officers and committee chairmen:

Chairman—Orval W. Daniels
First Vice-Chairman—W. E. Bush
Second Vice-Chairman—Roy Whitt
Secretary—Don G. Morgan
Treasurer—J. C. Frick
Chairman, Membership Committee—Bob Scott
Chairman, Program Committee—W. E. Bush
Technical Representative—P. D. Carter, 625 S. Fountain, Wichita

Election of Officers

York, Pa.—The following officers and committee chairmen have been elected and appointed by the *York-Central Pennsylvania Section*.

Chairman—Frank A. Logue, Alloy Rods Co.
First Vice-Chairman—Paul L. Stumpf, S. Morgan Smith Co.
Secretary—G. Earl Jennings, S. Morgan Smith Co.
Asst. Secretary—Emily Light
Treasurer—Charles W. Allen
Chairman, Membership Committee—Charles H. Pyle
Chairman, Program Committee—Paul Stumpf, S. Morgan Smith Co.
Technical Representative—R. K. Lee, 521 Colonial Ave.

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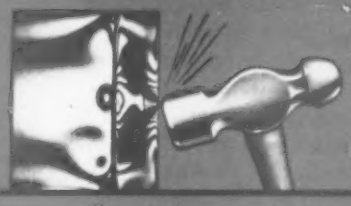

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(Continued from page 1116)

people in Europe live in apartments yet they are not accused of having no home-life.

"I certainly see no evidence of this lack of home-life in my grandsons at Stamford, nor in the children of my American friends. When the entire life of many households revolves around television I find it difficult to understand how you could possibly be accused of no home-life.

"One of the many things I find difficult to understand is the mental attitude of those who receive aid from your very generous country. My country is not among the receivers so I am able to look at it objectively. There appears to be some deep rooted impression that America has vast quantities of wealth for which she has no use whatsoever. How otherwise intelligent people can subscribe to this idea is beyond me—but they do. Ask them where it comes from and they'll reply vaguely 'Oh, America has plenty.' They do not seem to realize that every dollar bill they receive has come from someone's paycheck.

"A great deal of this faulty thinking—it certainly couldn't be called reasoning—comes from the different outlook. You are in a big, big, country—you are used to vast spaces, large buildings; tremendous production which is needed to feed, clothe and house your hundred and sixty million people. You think big—you have to. You have grown up in a hurry; you have accomplished in a few hundred years what took most nations as many thousands. How many of the nations with histories and traditions stretching into the dim and distant past have achieved world leadership? You have—and within the lifetime of the youngest present today. You have got to think big and this is where misunderstanding arises.

"You have a different outlook on life. When machinery which would improve production was first installed in Britain, rioting broke out and the workers smashed the machines because they were afraid of unemployment. In France, machinery was regarded in the same light and the workers' wooden sabots were thrown into the working parts, which incidentally, gave to the world that very descriptive term 'sabotage.'

"What did Americans do when the machinery era arrived? They stole into the barn where the first cotton gin was installed, but did they smash it to pieces? No, gentlemen, not at all, they borrowed it so that they too could increase production.

"When I see the speed with which a building is demolished and the rapidity another rises, I see even more clearly the many obstacles to a better understanding. It could be you move too fast for the more leisurely tempo of other countries.

"In your anxiety to show your guests as much as possible—and I am speaking more of official visitors than personal guests—you take them on a fast moving tour working to a tight schedule. Distance means nothing, but every minute must count. The visitor thus gets the impression that this is the American way of life and returns home rather exhausted and thoroughly bewildered.

"Our problem is to make them see a different viewpoint, to visualize the American as he really is, hospitable, generous and very enthusiastic.

"How many abroad would believe that the busy New York editors of internationally known magazines would take time they could ill afford to honor an old man?"

OBITUARY

Roy F. Johnson

Roy F. Johnson, 60, manager of Pullman-Standard Car Manufacturing Co.'s sales and service engineering division, died of a heart attack on Sunday, September 6th, aboard his sailing yacht "Lone Scout" 16 miles out of Michigan City, Ind., while participating in the Tri-State yacht race from St. Joseph, Mich.

Mr. Johnson, widely known in railroad engineering circles who joined Pullman-Standard in 1934, is survived by his widow, Martha, and a son, Lt. (jg) Thomas Johnson, a naval officer in Norfolk, Va., who was sailing the 30-sq m racing boat when his father suffered his fatal attack.

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Supplement to the Welding Journal, November 1953

Biaxial Fatigue Properties of Pressure Vessel Steels

Investigations conducted under the auspices of the Pressure Vessel Research Committee, WRC, to determine the relative abilities of carbon and low-alloy steels to resist cyclic loading with and without stress raisers imposed by design and fabrication

by C. E. Bowman and T. J. Dolan

Synopsis

Studies were made to determine the resistance of a carbon steel and of a low-alloy steel plate to fracture under repeated loading and to evaluate the relative strength reduction resulting from stress raisers imposed by design, fabrication or handling (such as welds or notches). Rectangular specimens of $^3/_4$-in. plate were uniformly loaded as simply supported flat slabs to develop a state of biaxial tension in the top surface of the central zone of the plate, the principal stresses having a ratio of about 2 to 1. Thus the surface of the specimen simulates the state of stress in the shell of a cylindrical pressure vessel. Special loading fixtures and pumping equipment were constructed to repeatedly apply a hydraulic load to the specimen.

In general, it was found that: (*a*) the fatigue life of A302 steel was appreciably greater than that of the A201 steel when subjected to cyclic strains up to those that exceeded the yield point strain of the A201 steel by as much as 75%, but at higher strains this trend was reversed; (*b*) a submerged-arc weld, ground flush with the parent metal, did not reduce the fatigue resistance of either type of steel; and (*c*) a notch having a theoretical stress concentration factor of 2.8 reduced the number of cycles of repeated load necessary to cause failure of both steels by factors of 4 to 16 times depending upon the particular stress level and material involved.

C. E. Bowman is Assistant Professor and **T. J. Dolan** is Head of the Department of Theoretical and Applied Mechanics, University of Illinois, Urbana, Ill.

Presented at the 34th AWS National Fall Meeting, Cleveland, Ohio, week of Oct. 18, 1953.

INTRODUCTION

SOME pressure vessel designers believe that vessels designed, constructed and operated under the most favorable conditions can safely be fabricated from available high-tensile alloy steel with design stresses of $^5/_8$ to $^2/_3$ yield strength instead of $^1/_4$ minimum specified tensile strength now permitted by ASME codes.[1]

The Pressure Vessel Research Committee has established a research program to accumulate experimental evidence on which to appraise the adequacy of design methods by obtaining a better knowledge of the properties of steel plate in tests that simulate service conditions. As one phase of that program, a research program is being conducted at the University of Illinois to study the relative fatigue properties of $^3/_4$-in. thick plate subjected to biaxial tension, the principal stresses having a ratio of 2 to 1. Experiments were conducted using A201 Grade A and A302 Grade B steel specimens to determine the relative strengths of the two metals when subjected to large strains that cause failure in a small number of cycles.

In the experiments reported in this paper the majority of plates failed by cracking after less than 500,000 loading cycles in contrast to conventional fatigue tests that are run to many millions of cycles. At the highest pressures, failure occurred in as few as 4000 cycles; however, as the cyclic pressure was decreased, the fatigue life increased up to a maximum of about a million cycles in a few cases. It was felt unnecessary to con-

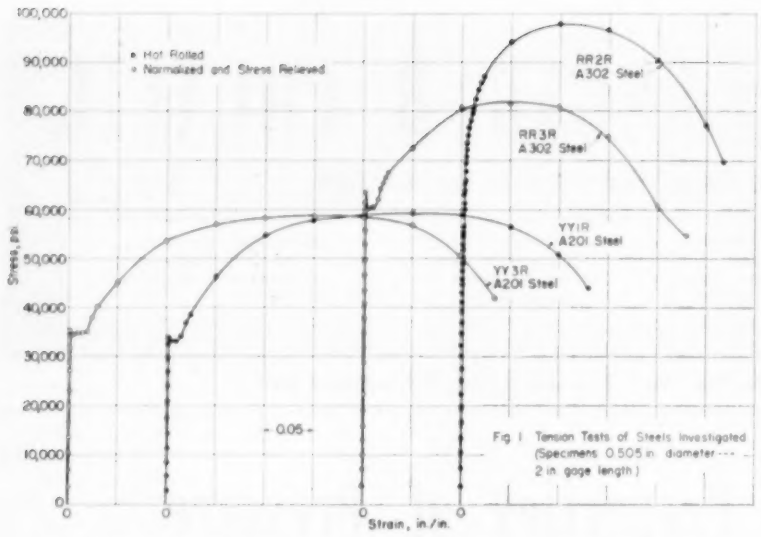


Fig. 1 Tension tests of steels investigated (specimens 0.505 in. diam, 2-in. gage length)

tinue stressing a boiler plate or other pressure vessel steel beyond one million cycles because with few exceptions most classes of vessels are seldom exposed to more than a few thousand (or perhaps several hundred thousand) load cycles during their lifetime.[1]

In addition to the basic repeated load studies of the normalized and pickled plate the reduction in strength caused by stress raisers in the form of small machined notches was investigated and specimens with a longitudinal submerged-arc weld bead were tested to determine whether deleterious effects were developed by welding operations. The testing equipment and loading fixtures were designed to accommodate a wide variety of plate specimens as well as plates with welded attachments or other simulated pressure vessel details (which will be included in the research program at a later date).

The essential features of the fatigue test consisted of fitting a piece of $^3/_4$-in. plate approximately $9^7/_8$ x 17 in. in size into a fixture that permitted hydraulic pressure to be applied to the bottom face of the plate. The plate was freely supported around the four edges. The tensile stresses that developed in the central portion of the top face of the plate during loading were twice as large in the narrow direction of the plate as those in the longitudinal direction. The equipment repeated a pressure cycle of zero to maximum at the rate of about 100 cpm in each of two test fixtures that were alternately pulsed.

MATERIALS AND SPECIMENS INVESTIGATED

The two grades of $^3/_4$-in. plate studied had the chemical analyses listed in Table 1. Typical tensile stress-strain diagrams for these steels (utilizing standard $^1/_2$-in. diam cylindrical specimens) are shown in Fig. 1 for the as-rolled and the normalized conditions.

Each of the plate specimens was cut to the over-all size shown in Fig. 2 (*a*) with the direction of rolling parallel to the direction in which the maximum principal stress would be applied, and processed in the following manner: normalize at 1650° F, stress relieve at 1150° F, pickle* to remove scale and finally machine the edges of the top and bottom as shown in Fig. 2 (*a*) to conform to the test fixture design and accommodate an O-ring seal against the bottom face.

For plates with a longitudinal submerged-arc weld bead, the sequence of processing was as follows: normalize at 1650° F, mill V-groove along centerline as shown in Fig. 2 (*a*), lay weld bead, straighten plate, grind weld flush, stress relieve at 1150° F and finally

* The pickling bath operated at 150° F and contained a 5% solution of hydrochloric acid with 3 lb of Hibitite added per each 100 lb of concentrated acid used to make up the bath. Specimens were rinsed, neutralized and oiled after pickling.

Table 1—Chemical Analysis

Steel	*A201 Grade A*	*A302 Grade B*
Carbon, %	0.14	0.16
Manganese, %	0.38	1.44
Phosphorus, %	0.017	0.026
Sulfur, %	0.034	0.032
Silica, %	0.20	0.27
Molybdenum, %	....	0.43

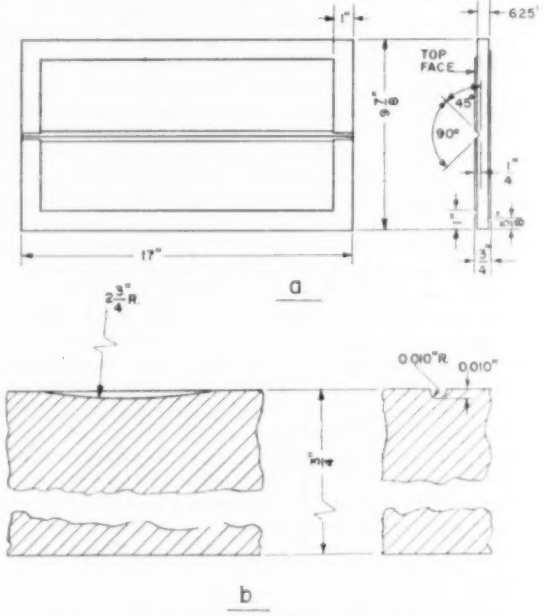


Fig. 2 Dimensional details of plate specimens

(*a*) Groove detail for submerged-arc weld bead. (All specimens had the same over-all size indicated in Fig. 2(*a*).) (*b*) Notch detail for notched specimens; located at geometric center of top surface of plate.

Table 2—Mechanical Properties of the Steels*

Steel→	—*A201 Grade A*—		—*A302 Grade B*—	
	As rcd.	*Normal-ized†*	*As rcd*	*Normal-ized†*
Yield point, psi	34,400	36,300	66,400‡	62,400
Tensile strength, psi	59,500	58,300	97,900	80,300
Elongation in 2 in., %	41 5	42 3	25 8	30 3
Reduction of area, %	62 5	66 5	61 7	67 4
Ratio yield strength to tensile strength	0 58	0 62	0 68	0 77

* All values shown are average of tests on two standard $^1/_2$-in diam cylindrical specimens whose axes were parallel to the direction of hot rolling.

† Normalized at 1650° F for 1 hr. All plate specimens were normalized before testing in fatigue.

‡ Yield strength at 0.2% offset. No definite yield point.

machine edges to adapt specimen to the loading fixture.

The weld bead was laid at 12 ipm using 800 amp at 30 v ac. A $^1/_4$-in. manganese-molybdenum rod Oxweld No. 40 was used. The welding caused the plates to bow slightly and consequently the plates were straightened before final stress relief. The excess weld metal was removed by milling and hand grinding flush with the original surface, the final grind marks being parallel to the direction of maximum stress (i.e., at right angles to the weld bead).

The notches selected for use in investigating the strength reduction caused by stress raisers were purposely made shallow in depth since it was only in the top surface of the plate that the biaxial stresses were known. The majority of notched specimens were formed by making a plunge cut into the center of the top surface of the plate using a $2^3/_4$-in. diam milling cutter of the type normally used for cutting the notch in a Charpy impact specimen. This formed a sort of "moon-shaped" indent (as shown in Fig. 2 (*b*)) having a root radius of 0.010 in., a maximum depth of 0.010 in. and a length of about $^3/_8$ in. The axis of the notch was aligned *perpendicular* to the direction of maximum stress

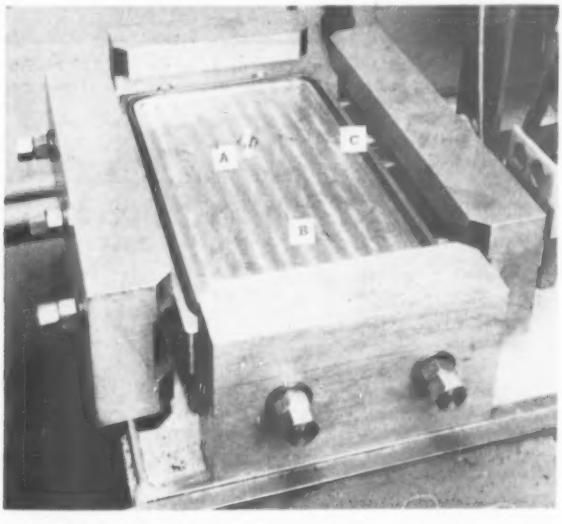


Fig. 3 Loading fixture opened before installing plate specimen

(*A*) Oil port. (*B*) Backing plate. (*C*) O-ring seal.

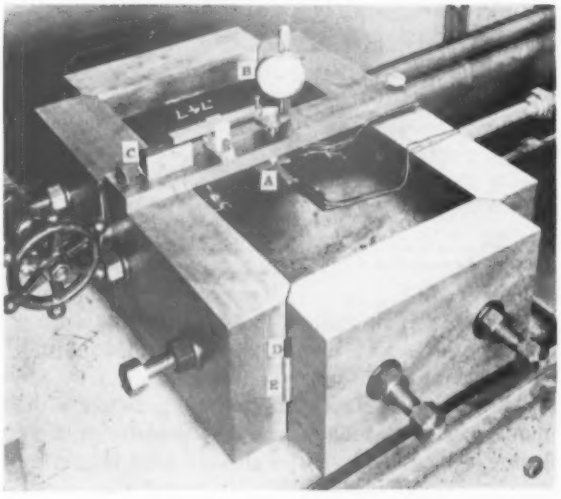


Fig. 4 Plate mounted in fixture

(*A*) Strain gages. (*B*) Deflection gage. (*C*) Safety switch to shut-off machine. (*D*) Edge of test plate. (*E*) Edge of backing plate.

(with its long axis parallel to the long axis of the plate) in one series of tests for each grade of steel. A limited study was also conducted using four plates of each steel with the notch *parallel* to the direction of maximum stress. The theoretical stress concentration factor for the notch as determined from Neuber's nomograph[2] is 2.8.

Two plates of each grade of steel with a less severe stress raiser were also prepared by milling a shallow notch in the form of a spherical dimple having $^1/_8$ in. radius but a depth of only 0.010 in. This was employed

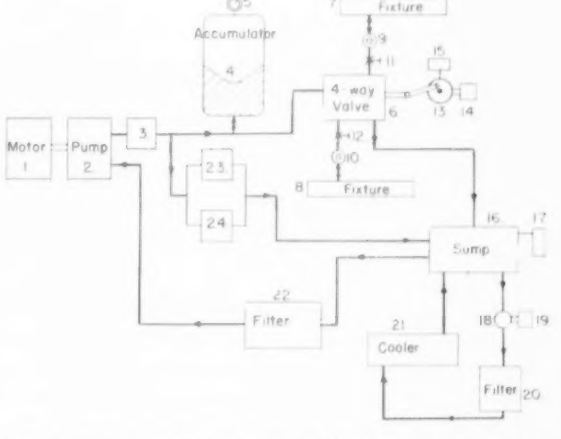


Fig. 5 Diagrammatic sketch of hydraulic load cycling equipment

(*1*) 15-hp, a-c electric motor. (*2*) Hydraulic pump, compensator controlled, variable discharge. (*3*) Check valve. (*4*) Hydraulic accumulator, 5 gal capacity, separator bladder, max operating pressure 3,000 psi. (*5*) Bourdon-type pressure gage. (*6*) Stem-operated 4-way valve. (*7*) Loading fixture No. 1. (*8*) Loading fixture No. 2. (*9–10*) Throttling orifice.

(*11–12*) Globe valve. (*13*) Motor-driven crank mechanism. (*14*) Air motor. (*15*) Revolution counter. (*16*) Sump. (*17*) Air filter. (*18*) Vane-type circulating pump. (*19*) $^1/_4$-hp circulating pump motor. (*20*) 10 micron filter. (*21*) Oil cooler (air). (*22*) 200 mesh filter. (*23*) Solenoid-operated pressure relief valve. (*24*) Spring operated safety release valve.

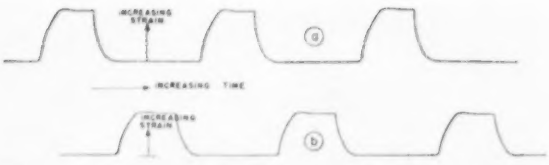


Fig. 6 Tracing of oscillograph record of loading cycles developed by the equipment when testing two plates simultaneously

Traces *a* and *b* are records of strains indicated by wire resistance-type gages at the centers of the two plates.

to study the effect of a mild stress raiser whose theoretical concentration factor was about 1.5

All plates were inspected for obvious defects such as punch marks, heat numbers or deep gouge or roll marks before testing. Any major defects were removed by grinding before the plates were tested because such sharp irregularities constituted notches of unknown severity.

TESTING MACHINE DESIGN

A unique hydraulic machine and special fixtures in which two plates were tested simultaneously was designed and built especially for the series of experiments. The plate specimen was mounted in a loading fixture as shown in Figs. 3 and 4 which is similar to the one described by Blaser, Tucker and Kooistra.[3] However to increase the speed of operation and facilitate control, a different pumping and cycling system was devised as is indicated diagrammatically in Fig. 5.

In general this consisted of a variable-volume, compensator-controlled pump feeding into a large size accumulator of the oil-nitrogen type. The pressure in the accumulator was maintained at any desired predetermined level (up to a maximum of 2500 psi) by means of the pressure controller that varied the output of the pump. Connected to the pressure system at the accumulator was a four-way spool type valve whose plunger was mechanically operated by a connecting rod and eccentric which was usually driven at 100 strokes per minute. The four-way valve was connected by means of short lengths of pipe to each loading fixture and the discharge from the four-way valve led to the sump. Thus throwing the plunger of the valve to one end of the stroke pulsed one fixture and drained the oil from the other back into the sump. Suitable mechanisms were incorporated to shut off the equipment in the event of excessive deflection or fracture of the plates, and a by-pass was arranged to release the pressure in the oil-nitrogen accumulator whenever the machine was shut off.

The operating characteristics such as the pressure-time cycle and the pressure regulation were very satisfactory. Oscillograph records of the type in Fig. 6 showed that when operating at 100 cpm there was sufficient time for the pressure in each fixture to reach and maintain operating pressure and to drop to and maintain zero pressure during each cycle. The maximum variation in the operating pressure was less than 2% throughout any given experiment. The machine required little maintenance during the entire research program which involved over 15 million cycles of operation

EXPERIMENTAL DATA

Since it was desired to produce cracking in a relatively small number of cycles, it was necessary to subject the plates to stress levels in the region of and above the yield point of each metal. At the higher stress levels this resulted in considerable plastic deformation occurring in the plate specimens during the first few cycles of operation. By means of a wire resistance-type strain gage attached to the surface of the plate as indicated in Fig. 4, and by means of the deflection measurements obtained from an Ames dial mounted at the center of the plate, a careful record of the plastic action and of the cyclic strains could be obtained during the progress of each experiment. It was found that when plastic deformations occurred, the deflections increased very little after the first 100 or 200 cycles; the cyclic variation in strain (the strain occurring as the pressure

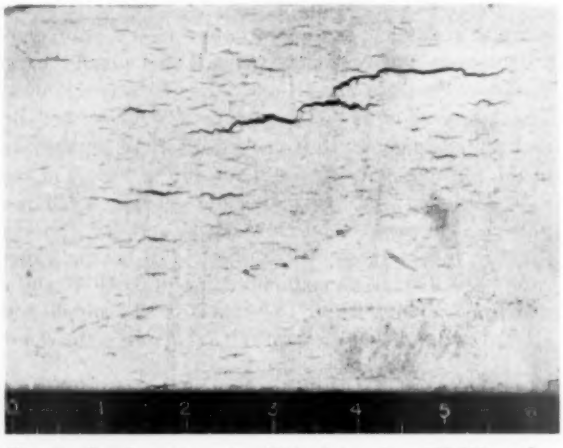


Fig. 7 Crack pattern in 4201 plate after 123,220 cycles. The plate was subjected to 3110 micro-in./in. strain per cycle

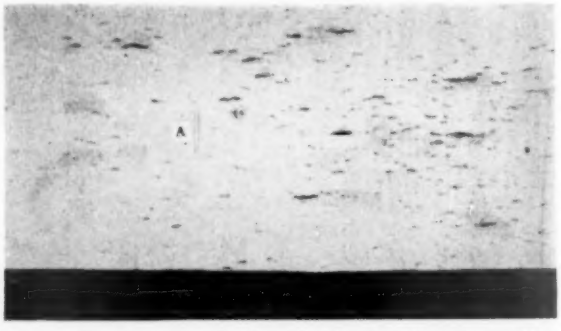


Fig. 8 Crack pattern developed after 167,000 cycles in 4302 plate containing machined notch aligned parallel to direction of maximum stress

The plate was subjected to 2176 micro-in./in. strain per cycle. (4) Notch in geometric center of plate.

was applied to the plate from zero to a maximum) was recorded and plotted as indicative of the significant variation of stress. Gross, Tsang and Stout[1] have shown that the cyclic variation in strain is of primary importance in determining the fatigue life rather than the mean strain involved; hence, the data in this paper have been plotted in terms of alternating strain without regard to variations in the mean plastic strain that occurred upon initial loading of the specimen.

The operation in the loading fixture is dramatic in revealing the extreme toughness and ability to withstand punishment exhibited by the steels. For example, in testing A201 steel at a cyclic strain approximating twice that required to produce initial yielding, a permanent plastic deflection of over $^1/_2$ in. occurred in the specimen during the first few applications of pressure. Thereafter the inherent toughness of the material allowed a cyclic deflection of about $^1/_{16}$ in. on every application of pressure; however, under these severe conditions the plate was able to withstand nearly 70,000 cycles before minute cracks developed in the surface. The plate actually appeared to behave like a piece of rubber because of the large visible deformations that occurred.

Since the action in the plate is primarily that of biaxial bending, a stress gradient exists throughout the thickness of the plate and only the top surface of the plate is regarded as representative of the desired state of stress. Hence it was not intended to propagate the fracture completely through the thickness of the plate, but rather to define failure as the appearance of a crack in its surface. In order to select a criterion of failure, it was deemed desirable to standardize on a crack of sufficient length to minimize errors in detection of the first microscopic crack that formed and to incorporate some stages of the early processes of crack growth that might be of importance in a pressure vessel. As a compromise a crack length of $^3/_{16}$ in. was selected as constituting failure; however, most of the experiments were continued to higher numbers of cycles to watch the formation and development of cracks that occurred.

Several different methods and devices were employed in the early experimentation to assist in detection of the cracks in their early stages of formation; visual observation with low-power microscopes proved to be rather difficult on the dark rough surface of the unmachined plates. However it was found that a coating of white lead thinned with machine oil to the consistency of a thick paint and brushed uniformly over the surface of the specimen was very effective in detecting cracks at an early stage of development. The formation of even a microscopic crack 0.010 in. long developed a black streak in the white lead coating and made visual inspection for cracks a simple, effective process. Upon

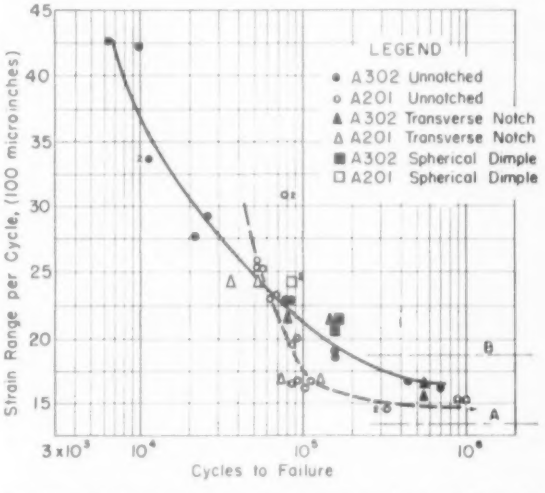


Fig. 10 Strain range per cycle versus cycles to failure for plate specimens of A201 and A302 steel

Transverse notches were those perpendicular to the *minimum* principal stress. *A* and *B* indicate yield points of plate specimens of A201 and A302 steels, respectively.

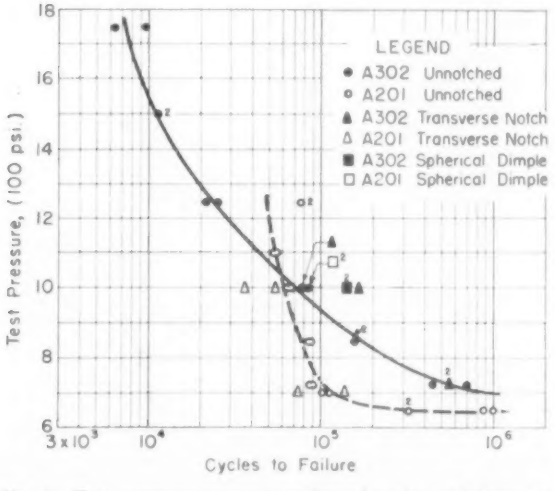


Fig. 9 Test pressure versus number of cycles to failure for plate specimens of A201 and A302 steel

Points marked *2* represent two specimens. Transverse notches were those perpendicular to the *minimum* principal stress.

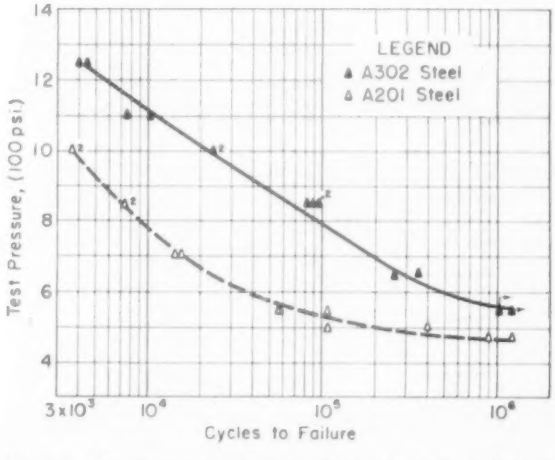


Fig. 11 Test pressure versus number of cycles to failure for notched plate specimens of A201 and A302 steel

Using notch of type shown in Fig. 2(*b*). Points marked *2* represent two specimens.

completion of each experiment a fresh coating was applied and the machine operated for a few moments before the plate was removed to bring out a well-defined crack pattern that could be readily photographed.

Figures 7 and 8 illustrate the types of crack patterns that developed in the plate specimens as indicated by the white lead coating. The cracks were usually very numerous, the longer cracks developing as several short cracks ran together. Cracks generally developed in random locations on the top surface of the plate but were always longitudinal and usually occurred within 4 in. of the center of the plate in the longitudinal direction and within 2 in. of the center in the transverse direction. At high stress levels many small cracks formed and developed rapidly whereas at lower stress levels fewer cracks were initiated and grew somewhat slower.

In detecting failure of the notched specimens it was found useful to use a small Brinell microscope positioned directly over the center of the notch to periodically inspect and watch the growth of the cracks which initiated at the root of the notch and gradually spread in the longitudinal direction out into the body of the plate.

The principal experimental data are shown in the illustrations of Figs. 9 to 14 which compare the fatigue properties of the two grades of steel. It will be noted that the data for each type of specimen are plotted first in terms of the *test pressure* employed in the fixture (Figs. 9, 11 and 13) and, secondly, in terms of the *strain range per cycle* that occurred during each cycle of operation as the plate was loaded (Figs. 10, 12, 14).* In each case where the strains are plotted, these represent the maximum principal strain measured in the center of the plate in the direction parallel to the narrow edge of the plate. For the notched specimens these strains were measured in the surface of the plate immediately adjacent to the notch and should be regarded as nominal values only since they are not equivalent to the actual strains at the root of the notch.

* Strains are shown in micro-in./in.; that is, 1000 micro-in. = 0.001 in. measured on a 1-in. gage length.

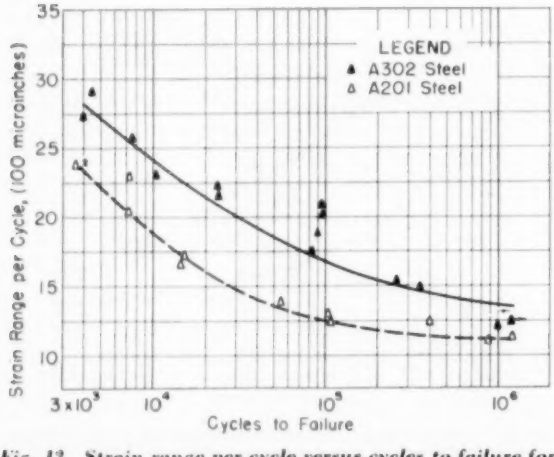


Fig. 12 Strain range per cycle versus cycles to failure for notched plate specimens of A201 and A302 steel (using notch of type shown in Fig. 2(b))

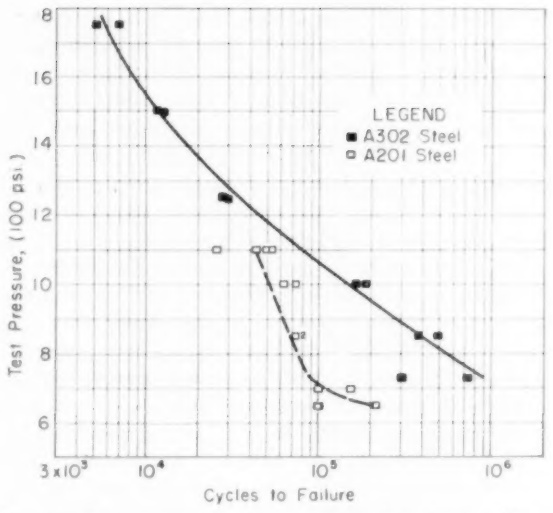


Fig. 13 Test pressure versus number of cycles to failure for welded plate specimens

DISCUSSION OF DATA

A comparison of the data for unnotched specimens of A201 and A302 steel plate shown in Figs. 9 and 10 indicates that A302 steel had a fatigue life more than twice that of the A201 steel at strain levels slightly above yield point strain of the latter. However when these plates were pulsed at a pressure which developed a very large range of strain in each cycle (that is, above 2300 micro-in./in.), the fatigue life of the A302 steel was no longer superior to that of the A201. It is interesting to note that this same trend was shown in the results of bending fatigue tests of small specimens of the same metals in data reported by Stout and coworkers.[4,5]

The 0.010-in. radius notch aligned perpendicular to

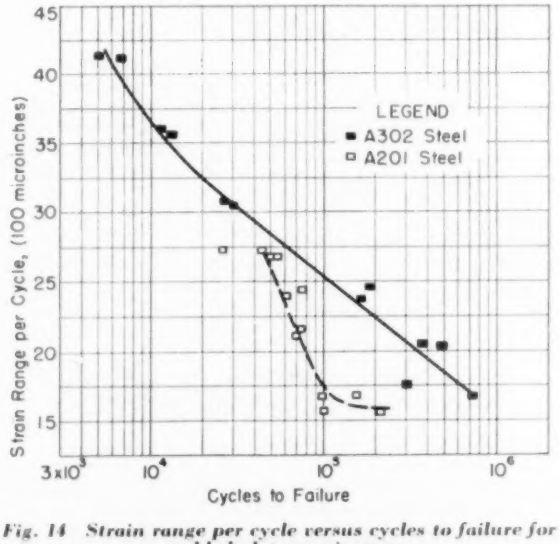


Fig. 14 Strain range per cycle versus cycles to failure for welded plate specimens

the direction of maximum stress materially reduced the fatigue strength of both types of steel as indicated by Figs. 11 and 12. It will be noted that the notched specimens of A302 steel exhibited fatigue lives that were 6 to 10 times as long as those of the A201 steel at the same test pressure, or from 3 to 10 times those of the A201 steel when compared at the same range of *strain*. Furthermore, at all levels of stress the notched specimens of A201 steel exhibited strengths considerably below those of the A302 steel which is in contrast to the tendency exhibited by the *unnotched* specimens at high strain levels.

The data shown in Figs. 13 and 14 indicate that the relative fatigue lives of the two grades of steel with a longitudinal submerged-arc weld bead were similar to those of the same plates without the weld. In general, the submerged-arc weld bead was stronger than the plate and did not introduce deleterious effects in the heat-affected zones of the plate adjacent to the weld. This comparison is shown somewhat more clearly in

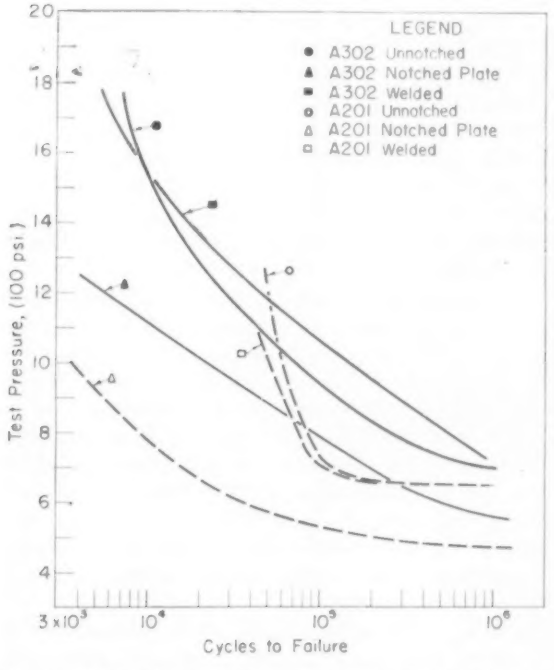


Fig. 15 Comparison of test pressure versus cycles to failure

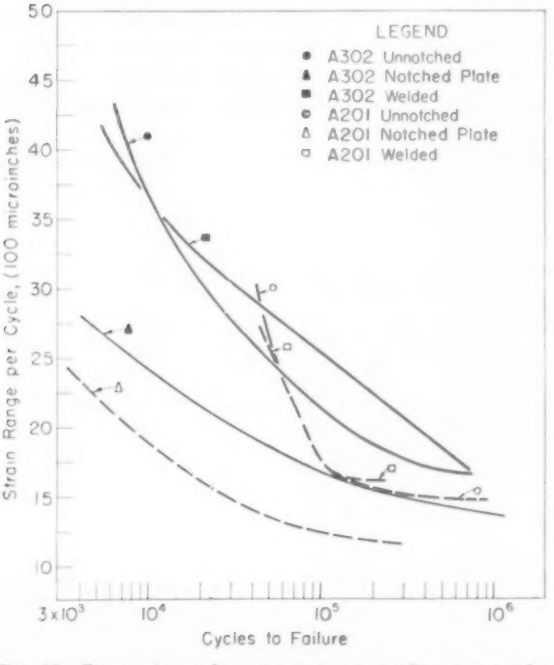


Fig. 16 Comparison of strain range per cycle versus cycles to failure

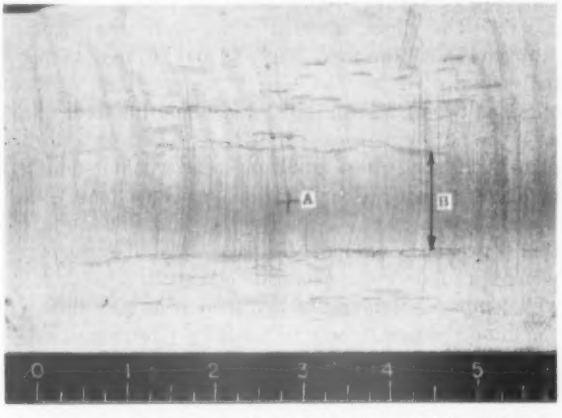
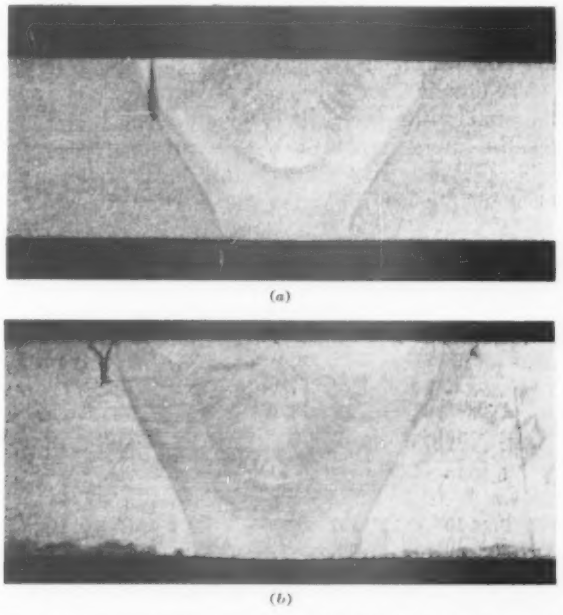


Fig. 17 Crack pattern developed after 22,860 cycles in A302 plate containing longitudinal submerged-arc weld

The plate was subjected to 3600 micro in./in. strain per cycle. (*A*) Geometric center of plate. (*B*) Width of weld metal.



(*a*)

(*b*)

Fig. 18 Etched cross sections of welded plate after test showing cracks developed

(*a*) Steel A201 Grade A. (*b*) Steel A302 Grade B.

Figs. 15 and 16 in which the average curves from the previous illustrations are compiled for comparison. In these figures it will be observed that the specimens with a weld had fatigue lives as long or slightly longer than those of the plate without welds.

The cracks that developed to failure in plates containing a weld bead were generally found to initiate at the junction between the weld metal and the heat-affected zone although cracks formed in other parts of the central region of the plate as is indicated in Figs. 17 and 18. No cracks were observed in the weld metal of any of the 24 plates that were so tested. This is a good indication that the strength of the weld bead was higher than that of the base plate and the deposited metal did not contain any major defects that would serve as abnormal stress raisers. The slight increase of life indicated for the A302 steel with the weld (Figs. 15 and 16) may be partially due to the narrow zone of semipolished surface obtained during machining and grinding the weld flush with the adjacent plate. However, the stresses in the plate at the zone where failure occurred would nominally be slightly higher than those in unwelded plate since a slight tendency existed to form a "kink" at the junction between the weld and the weaker metal in the plate which was undergoing plastic deformation.

A comparison of the notched specimens with those of the unnotched plate may be made by referring to Figs. 15 and 16. The curves shown indicate that the small machined notch reduced the fatigue life of the A302 steel plates by factors of 2 to 7 and the A201 steel plates by factors of 6 to 10 depending somewhat upon the strain levels involved. However, a more appropriate criterion of fatigue behavior might be a comparison of the relative strengths for a given life since these data are more appropriate for design purposes and also are subjected to less inherent scatter than are values based upon the fatigue life.

The data in Table 3 list both the pressure range and the strain range per cycle that both notched and unnotched specimens of the two steels withstood at several arbitrarily selected lifetimes. In general, the data in Columns 5 and 6 show that the effective strength reduction caused by the notch in the A201 steel was slightly greater than for the A302 steel, but in each case the decrease was small compared with the theoretical stress concentration factor of 2.8 for the notch employed. Furthermore, the strength reduction seemed to be slightly greater for the test conditions employing very large strains to produce failure in 10^4 cycles as compared with the data for longer fatigue life.

A comparison of the ratios listed in Columns 7 and 8 of Table 3 shows the superiority of the A302 steel over the A201 steel to be more pronounced for the notched condition. However these ratios (that indicate the relative strengths of the two steels under repeated loading) appear to be approximately comparable with the relative tensile strengths (a ratio of 1.38 for the two steels); the values tabulated were considerably lower than the ratio (1.72) of the static tensile yield points of the two steels.

In interpreting the data no attempt has been made to convert the results to "stresses" since no complete rational method is available to determine these for the case of biaxial conditions in the plastic range. However, it is felt that either the pressure employed in testing the plate or the principal strain range developed in the surface of the plates are suitable criteria on which to judge the relative performance. Baron and Larson[6] in axial load tests of similar steels concluded that the fatigue lives were more closely related to the tensile strains than to the average stresses or the maximum stresses in the specimens.

In order to check the relative effect of orientation of the notch, tests were made on four plates of each grade of steel in which the notches were aligned perpendicular to the direction of the *minimum* stress. The test data for these special notches are shown by triangles in Figs. 9 and 10. The stress concentration developed by the notch in this direction was not sufficiently severe to cause a crack to form perpendicular to the direction of *minimum* stress and the notch did not materially affect the fatigue life of the specimens. A photograph of a typical failure in the plate containing a notch in this transverse orientation was shown in Fig. 8 where it will be observed that all the cracks were per-

Table 3—Relative Resistance to Repeated Loading

Cycles to Failure	*Strain range per cycle, micro-in./in.*				*Reduction due to notch*		*Relative strength of the two metals*	
	Unnotched		*Notched*		*Col. 1 / Col. 3*	*Col. 2 / Col. 4*	*Col. 1 / Col. 2*	*Col. 3 / Col. 4*
Steel→	*A302*	*A201*	*A302*	*A201*	*A302*	*A201*	*Unnotched*	*Notched*
	(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)
1×10^4	3680	....	2420	1890	1.52	....	....	1.28
1×10^5	2140	1760	1680	1250	1.27	1.41	1.21	1.34
5×10^5	1680	1480	1430	1130	1.18	1.31	1.14	1.27
	Pressure range per cycle, psi							
1×10^4	1540	....	1115	780	1.38	....	....	1.43
1×10^5	940	730	790	530	1.19	1.38	1.29	1.49
5×10^5	735	660	600	480	1.23	1.38	1.11	1.25

Static tensile yield point: Ratio = $\frac{YP \text{ A302}}{YP \text{ A201}} = 1.72$

Static tensile strength: Ratio = $\frac{TS \text{ A302}}{TS \text{ A201}} = 1.38$

pendicular to the *maximum* principal stress and none of these seem to have originated from the notch itself.

The experimental results for the few specimens that were tested with a mild stress raiser in the form of a small spherical dimple that was 0.010 in. deep are shown by squares in Figs. 9 and 10. Here again it was found that the failure initiated in the original rolled surface of the plates and the notches themselves had little if any influence in initiating or governing failure of the specimens. Thus one might postulate that the degree of roughness and other surface defects existing in the rolled and pickled plate amounted to an effective stress raiser equivalent to or greater than that produced by a small geometrical irregularity that would normally cause a 50% increase in stress. Also, the surface metal which was subjected to some slight degree of decarburization during rolling and heat-treating operations probably is in a slightly weaker condition than the metal which existed just below the surface at the root of the notch.

SUMMARY

1. The unique hydraulic fatigue testing machine designed and built in the initial phase of this research proved to be a very effective means of developing and controlling the biaxial state of stress desired.

2. The fatigue properties of the A302 steel were superior to those of the A201 steel at strain levels well above the yield point of the A201 steel. However, at high levels of cyclic strain (in the region of twice the yield point strain of the A201 steel and above) the A201 steel tended to exhibit superior life when tested in *unnotched* specimens.

3. A small longitudinal notch machined in the center of the plate with a theoretical stress concentration factor of 2.8 developed an effective strength reduction only about half this amount. The fatigue strength of notched specimens of A302 steel was greatly superior to that of notched specimens of the A201 steel at all levels of cyclic strain.

4. The relative fatigue life and fatigue strength of the two grades of steel with a submerged-arc weld bead were as great as those of the same plates without welds. The presence of a sound submerged-arc weld (machined flush as in these experiments) should not have a weakening effect when present in a pressure vessel.

5. The presence of mild notches or of notches that are not oriented perpendicular to the maximum principal stress did not exhibit a significant weakening effect in fatigue tests of the plate.

6. The relative resistance of the two grades of steel to repeated loading appeared to be more nearly comparable to their tensile strength ratio than to their relative yield points.

ACKNOWLEDGMENTS

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The research was conducted in Talbot Laboratory by personnel of the Theoretical and Applied Mechanics Department of the University of Illinois.

The execution of the work was made possible by the cooperation of H. T. Musgrove and J. M. Robertson, Laboratory Mechanics, of the department staff.

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Transition Temperatures of Structural Steel Beams with Butt-Welded Splices

An investigation of the resistance of butt-welded and unwelded structural beams to impact loads at low winter temperatures including relative behavior of semikilled and fully killed steels, low-hydrogen versus ordinary electrodes, with and without cope holes

by William J. Krefeld and George B. Anderson

Scope

This investigation was primarily concerned with the resistance and transition temperatures of structural steel beams, composed of semikilled and fully killed steels and fabricated with butt-welded splices. Static and impact tests on 16-in. WF 71-lb beams, in addition to confirming the favorable effect of aluminum deoxidation in lowering the transition temperature of the as-rolled base metal indicated by Charpy tests, furnished information on the relative behavior of the two grades of steel with splices as-welded, welded and "stress relieved" and when welded with preheat. The influence of these treatments was evaluated by increment drop impact tests on beams welded with E6011 and E6020 electrodes. Similar supplementary tests on beams composed of both steels when welded with low-hydrogen electrodes as-welded and with preheat, furnish comparative data. Other data deals with the character of fracture and the effect of flame-cut holes in the beam web. The influence of a sharp notch at a critical location in an unwelded beam and the tendency of crack propagation are indicated by the results of supplementary tests.

This investigation was conducted by the Civil Engineering Research Laboratories of Columbia University under the joint sponsorship of the Office of Naval Research, Contract No. N6-onr-271, T.O. 7 and the Welding Research Council of the Engineering Foundation, under direct supervision of the Structural Steel Research Committee of the Welding Research Council.

INTRODUCTION

THE present investigation is an extension of previous experimental studies[1] of the behavior of beams with butt-welded splices under impact, which dealt with the possible cumulative effect of residual stresses at strain rates associated with impact loading. Residual stresses of considerable magnitude were found in the vicinity of the weld,[2] which lowered the apparent proportional limit. There was no evidence of any adverse effect upon the ultimate behavior of the beams, which developed refusal loads by lateral buckling or web crippling, without fracture, at the test temperature of +70° F. A limited number of tests conducted at −40° F, however, resulted in complete brittle fractures through the welds.

The investigation here recorded was intended to determine the low temperature performance of butt-welded beams under impact loading, which afforded a comparison of the relative suitability of two grades of structural steel and the influence of thermal treatments of the weldments. The testing temperatures ranged from +70° F to −115° F which permitted determination of the critical temperature of transition from the normal ductile type of buckling failure to failure by brittle fracture.

The beams were rolled from heats of semikilled and silicon-aluminum fully killed steels. Charpy and Kahn tests indicated lower transition temperatures for the fully killed as-rolled base metal. In order to determine the effects of welding procedure, tests were conducted on beam specimens in which the weldments, made with E6011 and E6020 electrodes, represented three treatments; namely, (1) as-welded, (2) welded and stress relieved by postheating the entire welded beam to 1150° F for 2 hr, followed by furnace cooling to 500° F and final air cooling and (3) welded after preheating, with interpass temperatures of 350–400° F. Supplementary specimens of both base metals were fabricated with low-hydrogen (E6016) electrodes, both with and without preheat.

The low-temperature properties indicated by laboratory tests are influenced by the method of loading, geometry of the specimens and associated strain rate. In dealing with the material in a rolled section, the results of small-scale tests are further complicated by the variation of properties according to location in the sec-

William J. Krefeld is Professor of Civil Engineering, Director of Research, Materials Laboratory, Columbia University, New York, N. Y., and **George B. Anderson** is Research Associate, Department of Civil Engineering, Columbia University, New York, N. Y.

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Table 1—Chemical Analysis of Structural Steel Beams (Bethlehem Steel Co.)

	C	*Mn*	*P*	*S*	*Si*	*Ni*	*Cr*	*Mo*	*Cu*	*Sn*	N_2	*Al*
Semikilled steel (Heat 34J333)												
Ladle	0.22	0.58	0.017	0.034	0.06	0.06	0.06	0.03	0.14	0.012	...	...
Check	0.21	0.57	0.021	0.031	0.04	0.04	0.06	Nil	0.14	0.01	0.005	0.006
Fully killed steel (Heat 35J272)												
Ladle	0.19	0.72	0.023	0.031	0.23	0.07	0.04	0.03	0.14	0.017	...	...
Check	0.19	0.72	0.023	0.029	0.21	0.05	0.02	Nil	0.16	0.01	0.006	0.047

tion. Similarly, a comparison of the influence of welding involves the relative cooling rates of large and small specimens. The interpretation of test results from full size weldments, though satisfying with respect to particular design application, has limitations for generalized correlation purposes due to the influence of individual fabrication requirements, not otherwise reproducible. For example, the beam specimens used in the present studies, butt welded according to a specified procedure, include holes in the web adjacent to both flanges to facilitate the welding of the flanges. These flame-cut holes introduce local strain concentrations and indeterminate strain rates. In effect, the specimen includes an internal notch, complicated by the fact that the web weld terminates at this notch. Nevertheless, these beams represent a practical type of welded splice, whose behavior is of interest to the designing engineer, and afford comparison of the relative suitability of the two steels and welding treatments under the same design conditions. A limited number of additional comparative tests were made on a modified splice design eliminating the web holes.

It was considered desirable to proceed first with a series of "static" load tests with the hope of determining the relative low-temperature performance without elaborate instrumentation. However, the tests made on beams of both steels and different treatments at −115° F were inconclusive because all failures were caused by buckling. It was apparent that the determination of the relative tendency to failure by brittle fracture required either lower testing temperatures or higher strain rates. The lowering of the temperature below −115° F would have required a more elaborate refrigeration procedure. The testing under higher strain rates by increment drop impact tests were adopted for further studies. The testing procedures and results of both "static" (low strain rate) and impact tests are recorded in the following discussion.

MATERIALS

The beams used for these tests were 16-in. WF 71-lb sections, prepared by butt welding two 7-ft lengths to form a beam 14 ft long, for tests on a 12-ft span. The materials from which these beams were rolled represented two types of steel; namely, ASTM A7–46, semikilled steel (Heat 34J333) and ASTM A7–46, silicon-aluminum fully killed steel (Heat 35J272). Through the cooperation of the Bethlehem Steel Co., a complete history of the manufacture, fabrication and physical properties was recorded. A summary of the melting practice, mill practice and fabrication procedures as reported by the manufacturer is given in the Appendix. These heats provided 50 semikilled and 100 fully killed beams, 14 ft long, for this investigation.

The reported chemical analysis for these two steels is shown in Table 1.

It was the intention, in planning these tests, to include structural grade steels having appreciable differences in low-temperature properties as indicated by

Table 2—Physical Properties

*Specimen location**	*Yield point, psi*	*Yield strength, psi*	*Tensile strength, psi*	*—Elongation %— 8 in.*	*2 in.*	*Bend*	*McQuaid-Ehn grain size*
Semikilled, as-rolled							
Web, 4 in. from centerline (Beth.)	37,260	37,580	63,380	27.5	47.5	OK	100%, 1–3
Web, 5 in. from centerline	35,250	35,250	63,500	28.9	50.0	...	
Web, 1 in. from centerline	38,600	38,350	65,100	29.3	48.5	...	
Flange, 2 in. from edge	36,400	36,400	63,800	29.6	52.0	...	
Semikilled, stress relieved							
Web, 4 in. from centerline (Beth.)	37,260	37,400	62,190	28.7	49.5	OK	100%, 1–3
Fully killed, as-rolled							
Web, 4 in. from centerline (Beth.)	44,540	44,670	69,530	25.0	44.5	OK	100%, 6–8
Web, 5 in. from centerline	...	38,900	65,300	29.0	45.0	...	
Web, 1 in. from centerline	41,800	42,500	66,800	27.0	47.0	...	
Flange, 2 in. from edge	39,300	39,500	66,900	28.0	54.0	...	
Fully killed, stress relieved							
Web, 4 in. from centerline (Beth.)	43,920	44,190	67,160	27.5	49.0	OK	100%, 6–8
Web, 5 in. from centerline	...	38,600	66,500	26.4	49.0	...	
Web, 1 in. from centerline	42,700	42,700	68,300	26.8	46.5	...	
Flange, 2 in. from edge	40,000	40,500	66,700	29.0	54.5	...	

* Refers to location of center of 2-in. wide coupon; toe of fillet 6⅞ in. from center of web. All tests at +70° F.
REMARKS: Microstructure of both steels reported by manufacturer to be banded Sorbitic Pearlite and Ferrite.

conventional small specimen tests in order that these identifying properties might be compared with the behavior of full-scale weldments. The properties recorded included the standard tensile properties of various locations, Charpy V notch and keyhole notch impact transition data, and the results of Kahn tear tests. These properties were determined from specimens taken from the rolled beams and represented both as-rolled material and material after stress-relieving treatment at 1150° F for $^1/_2$ hr. Additional static tensile tests were made to determine the effect of temperature.

Table 2 records the results of standard tension tests on flat specimens taken from various locations of the web and flange. These results indicate higher yield point and ultimate strength for the fully killed steel, without appreciable difference in elongation. The yield strength was lowest for specimens taken from the web adjacent to the fillet for both steels. The properties do not appear to be appreciably affected by stress-relieving treatment.

Comparative Charpy and Kahn tests were made at the mill on specimens taken from the web about midway between the fillet and centerline. Because of the character of fracture of the welded beams it was of interest to investigate the variation of low-temperature properties by additional Charpy tests on specimens taken from the edge and center of the flanges and near the fillet and centerline of the web. All specimens were taken in the direction of rolling with notches on sides perpendicular to the original surface. The results of the Charpy and Kahn tests are shown in Figs. 1–5.

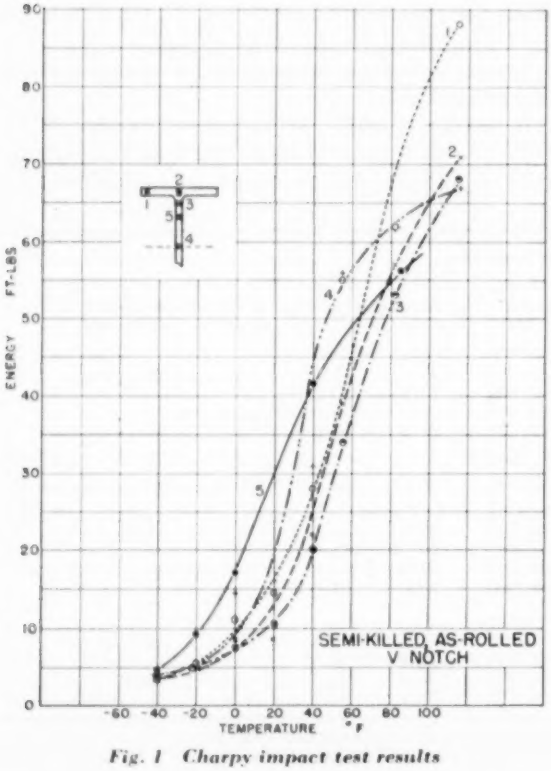


Fig. 1 Charpy impact test results

These curves indicate that the abrupt decrease in fracture energy occurs at higher temperatures for material taken near the fillet in both the flange and web. The curves for materials taken from the web between the fillet and centerline show intermediate energy values except that in all cases Curve *5* according to data submitted in the mill report shows considerably lower energy absorption at the higher temperatures. This may be a machine effect, possibly due to relative rigid-

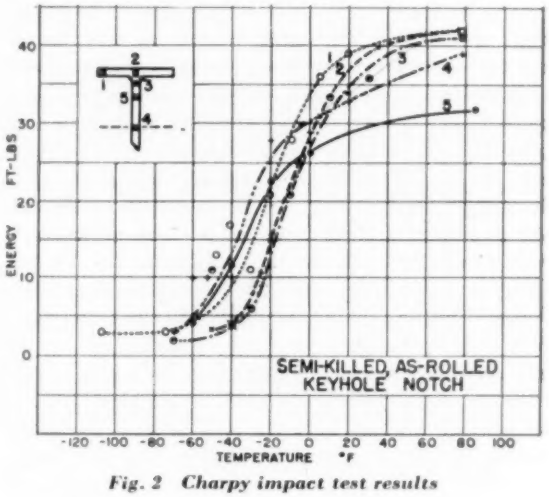


Fig. 2 Charpy impact test results

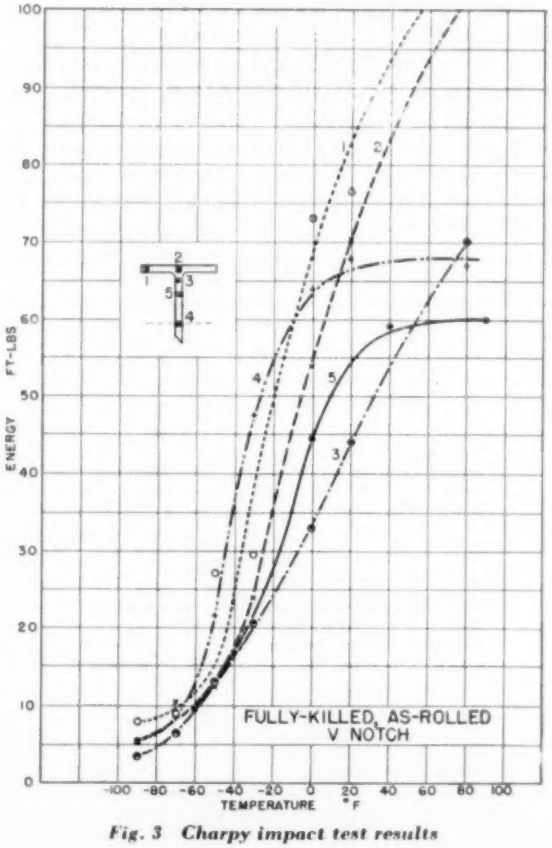


Fig. 3 Charpy impact test results

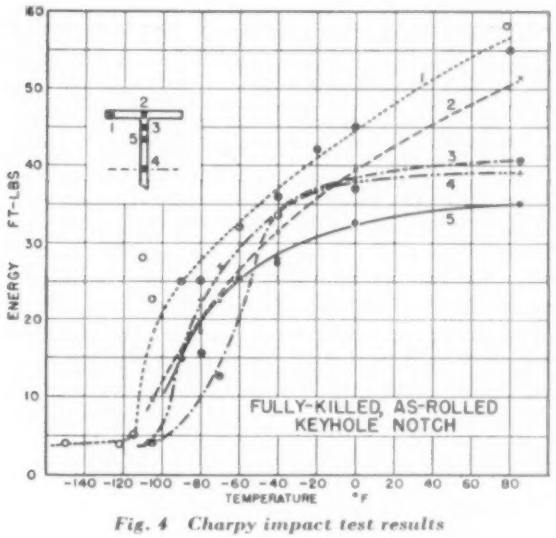


Fig. 4 Charpy impact test results

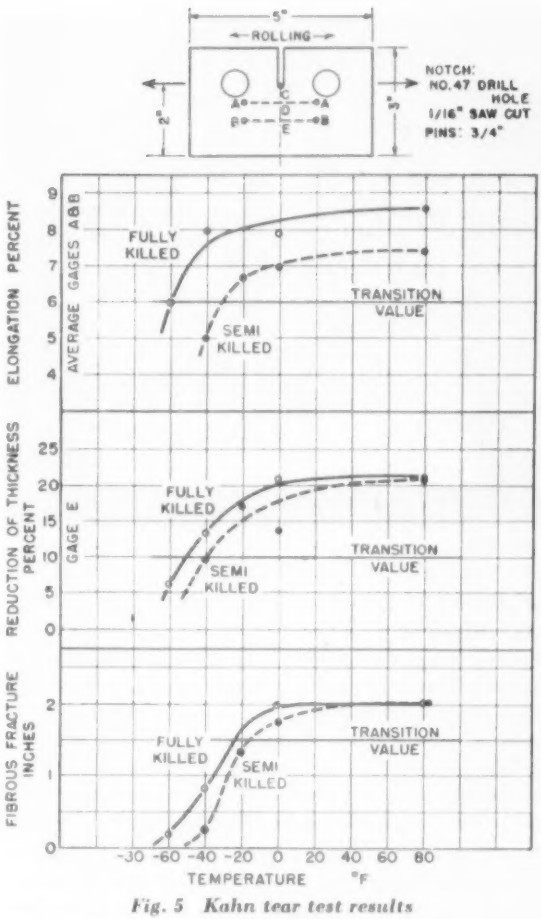


Fig. 5 Kahn tear test results

ities of the machines or supports which would have less influence at the lower fracturing energies. The energy values at the higher temperatures are not significant.

The transition temperatures derived from the Charpy tests are shown in Table 3. These temperatures are arbitrarily based on the 10 and 20 ft-lb levels for the keyhole notch and on the 15 and 50 ft-lb levels for the standard V notch. (Values at a common 15 ft-lb level are given for comparison.) The tabulated values show considerable spread for both types of notch depending upon location of the specimen, amounting to about 20° F for the semikilled steel and about 40° F for the fully killed steel. Under these circumstances a definite value of transition temperature cannot be assigned to either as-rolled steel but comparisons can be made of values at similar locations in the beam. The fully killed steel has transition temperatures based on the criteria shown, about 50 to 75° lower than the semikilled steel depending on location. It is evident that the silicon-aluminum deoxidation has decreased the notch sensitivity.

The greater spread of values indicated for the fully killed steel, particularly when based on the higher energy level criterion, is perhaps due to the character of fracture. In this steel, the fractures included numer-

Table 3—Transition Temperatures, Charpy Tests

Steel	*Test*	*Transition temperature, criterion-energy level*	*Transition temperatures, ° F* — *Flange* — *Edge (1)*	*Flange* — *Center (2)*	*Web* — *Near fillet (3)*	*Web* — *Center (4)*	*¼ points* (5)*
Semikilled							
As-rolled	Charpy keyhole	20 ft-lb	−22	−13	−11	−30	−25
As-rolled		10 ft-lb	−38	−26	−23	−46	−44
Stress relieved		20 ft lb	...	...	..	..	−22
As-rolled†		15 ft-lb	−29	−20	−18	−37	−34
As-rolled	Charpy V notch	50 ft-lb	+64	+70	+78	+47	+63
As-rolled		15 ft-lb	+17	+25	+33	+14	−4
Stress relieved		50 ft-lb	....	...	..	....	+60
Fully-Killed							
As-rolled	Charpy keyhole	20 ft-lb	−102	−79	−60	−84	−79
As-rolled		10 ft lb	−112	−103	−80	−95	−100
Stress relieved		20 ft-lb	....	...	..	..	−70
As-rolled†		15 ft-lb	−108	−93	−69	−90	−90
As-rolled	Charpy V notch	50 ft lb	−19	−5	+32	−26	+10
As-rolled		15 ft-lb	−50	−45	−42	−57	−45
Stress relieved		50 ft-lb	...	...	..	..	+25

* According to Bethlehem Steel Co. mill report data.
† For comparison with V notch at same energy level.

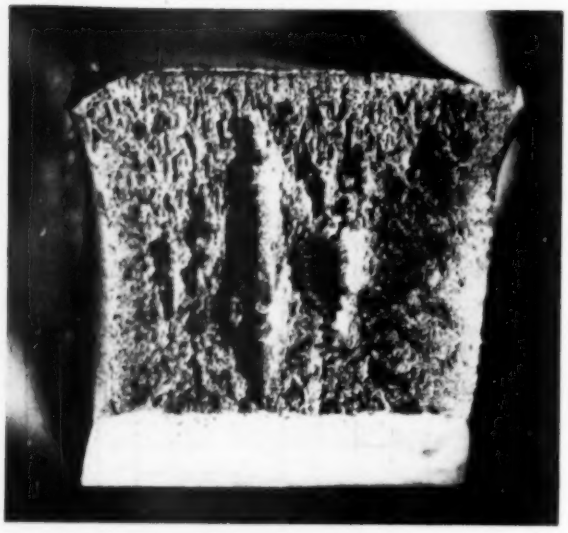


Plate 1 Fracture of Charpy specimen showing fissure

ous fissures which opened perpendicular to the direction of the notch. These fissures were found in all the fibrous fractures (higher temperatures), one of which is shown in Plate 1. In the intermediate transition range some fissures opened across a granular area and the fracture became fibrous immediately adjacent to the fissure. Probably due to lack of lateral restraint caused by the opening of the fissure, a more ductile local fracture resulted. These fissures were most pronounced in specimens taken from the center of the web and only occasionally in fractures of specimens taken from the edge of the flange. At lower temperatures, mixed results from a group of specimens showed that the presence of fissures raised the energy value from 4 to 25 ft-lb. At higher temperatures no relative values are available since all specimens developed fissures, but the curves for both notches indicate that the energy values were lowered. This opposite effect of fissures on the energy absorbed when large plastic deformations were produced at these higher temperatures is perhaps due to the relative volumes of material subjected to plastic deformation in a laminated as compared to a solid section. As in a simple tension test, the length of specimen affected by the "necking" before fracture is less for specimens with smaller sections, represented in this case by the multiple laminations.

The results of Kahn tear tests included in the mill report are shown in Fig. 5 and a summary of recorded data and transition temperatures based on various criteria is shown in Table 4. These data include final elongation in 2 in. on gage lengths located $^1/_2$ and 1 in. from the notch, reduction of thickness at the notch and $^1/_2$ and 1 in. from the notch, and the depth of section showing fibrous fracture. The transition values selected for the various measurements indicated a favorable influence of deoxidation but the temperature differences are not as great as for the Charpy tests.

The effect of temperature on the tensile lower yield point is shown in Fig. 6. Here again the data are subject to variations due to location of the specimens. The specimens were machined from adjacent $^3/_4$-in. wide strips cut longitudinally from one flange, located $1^3/_8$

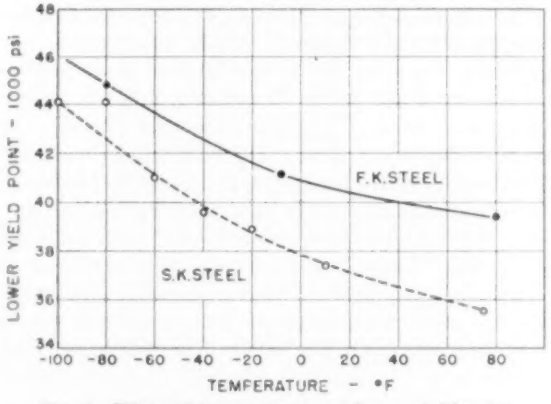


Fig. 6 Effect of temperature on lower yield point

Table 4—Summary of Kahn Tear Tests

Average values for specimens tested, according to Bethlehem Steel Co. report on materials—see Plate 5 for gage locations

No. specimens	*Temp., °F*	*Load, lb*	*—Elongation in 2 in., %— Gage A**	*Gage B†*	*—Reduction in thickness, % at: C*	*D*	*E*	*Type of fracture*
			Semikilled steel					
4	80	24,500	6.9	7.9	24.9	21.0	20.7	2-in. fibrous
2	0	26,300	7.0	7.0	24.1	18.1	13.8	$1^3/_4$-in. fibrous
3	−20	26,400	6.5	6.8	23.2	21.1	17.3	$1^1/_2$-in. fibrous
3	−40	26,900	5.5	4.5	25.2	15.6	9.6	$1^3/_4$-in. cryst.
			Fully killed steel					
3	80	28,030	8.0	9.3	24.8	21.8	21.3	2-in. fibrous
2	0	29,200	7.8	8.0	25.8	19.8	21.0	2-in. fibrous
3	−40	29,370	7.3	8.7	24.3	18.1	13.4	1.17-in. cryst.
3	−60	29,870	6.3	5.7	22.6	11.8	6.1	1.83-in. cryst.

Transition temperatures, °F, Kahn tear tests‡

Steel	*Fracture,*	*Elongation,*	*Reduction of thickness,*
Semikilled (AR)	−15	−30	−40
Fully killed (AR)	−23	−60	−50

* Gage line A-A located $^1/_2$ in. from center of hole.
† Gage line B-B located 1 in. from center of hole.
‡ Criteria: Elongation 6%, reduction of thickness 10%, fracture $1^1/_2$ in. fibrous.

and $2^1/_8$ in. from the flange edge. The specimens from the outside strip gave consistently higher yield points by about 2500 psi. The curves shown represent average values of 2 to 6 tests at a particular temperature. A few tension tests on the semikilled steel at −321° F showed a yield point of 116,000–120,000 psi. This value agrees reasonably well with a linear extension of the curve shown, to the lower temperature when the log of stress is plotted against $1/T$, *ABS*.

BEAM SPECIMENS

In preparation of the beams for butt welding, the ends of the web and flange to be welded were beveled by machine flame cutting to the dimensions shown in Fig. 7. The sections were mounted in a specially constructed positioning device which permitted rotation of the beams about their longitudinal axis, thus permitting welding in the flat position. The flanges were tack welded, about 1 in. at four points, located symmetrically about 2 in. from the edges, providing a root opening of $^1/_8$ in. The tacks were placed on the outer flange surface and removed by gouging during the welding operation. Except for the special series welded with low hydrogen electrodes, the arc welds were made with $^5/_{32}$-in. diam AWS-ASTM, E6011 welding rod for the root passes and $^3/_{16}$-in. diam AWS-ASTM, E6020 rod for the subsequent passes. The web joint was welded first by one operator. The number of passes, sequence and direction of welding are shown in the Appendix summary of the mill report. The coped holes at the ends of the web weld were enlarged by flame cutting after completion of the weld to assure sound metal at the end of the web welds.

The surface condition of the finished welds was good. Some irregularities existed in the hand-cut cope holes. In order to place SR-4 strain gages on the curved edge of these holes, the inside curved edge was smoothed by hand filing. In later tests when gages were not used, the holes were similarly smoothed to ensure comparable test conditions. The alignment of the welded segments was good but due to normal tolerance variations a small relative axial rotation of the two segments or sloping flange surfaces made it necessary to machine tapering bearing plates at the end supports in order to erect the beams with web vertical. A lateral concavity of the outer flange surface also required grinding of these surfaces at the supports to provide flat bearings. These precautions were necessary in preparing the specimens for impact tests in order to avoid eccentric load with resulting lateral vibrations and other effects which influenced the deflection measurements.

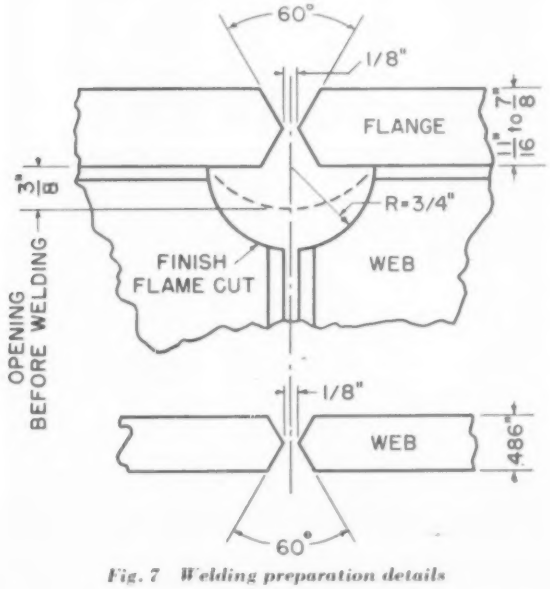


Fig. 7 Welding preparation details

TEST PROCEDURE

Static Tests

The beams were supported on heavy pedestals through 4-in. diam rollers between 2- x 12-in. plates at both ends, providing a 12-ft span. Load was applied through similar rollers at the third points of the span. Third-point loading was adopted to permit observation and measurement of strain distribution around the critical selection.

The entire beam, 14 ft long, was enclosed in an insulated chamber 18 in. wide, 28 in. high and 16 ft 8 in. long, inside. The chamber was composed of side, end, top and bottom sections with lapping joints, which were readily bolted together. The sections were made of sheet metal, enclosing a 3 in. thickness of insulation. Inside both ends of the chamber, a receptacle for dry ice was attached to the bottom section as shown schematically in Fig. 8. A blower with a 12-in. fan at both ends furnished an air stream through the dry ice receptacle and the insulated enclosure. This air stream returned to the blower through ducts incorporated in the sides of the chamber, exhausting the air from ports in the bottom of the side panels near midspan. The system thus circulated cooled air along each half of the beam. The rate of cooling depended upon the volume of dry ice used and velocity of the air stream. The beams, weighing 1000 lb, were cooled from +70 to −115° F in from 4 to 5 hr.

The supporting pedestals were insulated by similar panels. The openings in the top of the chamber for the third-point loading jig were sealed with flexible boots consisting of rubberized fabric and insulating wool, and the distributing beam was similarly insulated to reduce heat conduction to the cooling chamber.

The temperature of the beam was measured by copper-constantan thermocouples held in small drilled holes with solder. Temperature stations were located on both top and bottom flanges near the third points of the span and additional couples in the air stream at the ends were used for control purposes. The temperatures after a short stabilizing period were quite uniform, with only small differences between the upper and lower flanges. The test assembly was similar to that used for

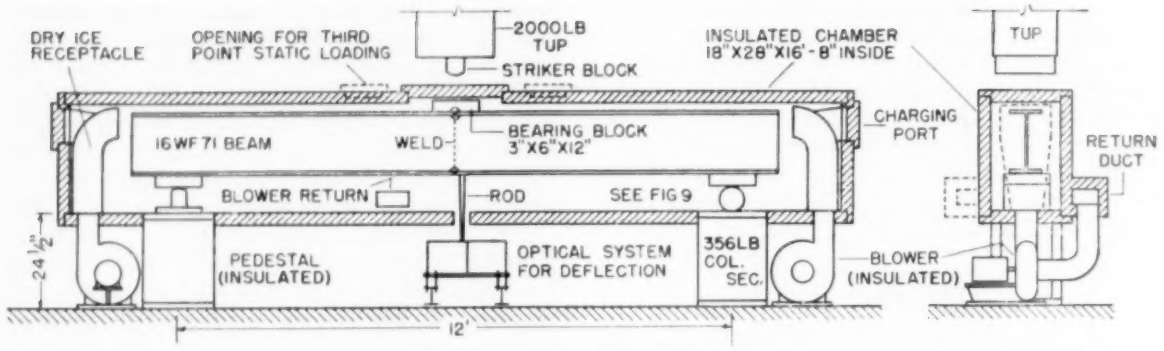


Fig. 8 Assembly for impact test

the impact tests shown in Fig. 8 with the exception of the third-point loading jig.

In order to measure the progress and distribution of strains across the section, SR-4 electric strain gages were placed longitudinally along the centerline of the weld after grinding the surface smooth, on the web and flange, and inside the cope holes. Gages were also placed at adjacent sections along the flanges. The center deflection of the lower flange was measured by reference to side bars attached on both sides to pins through the web over the supports. Attachment of the recording dial to a rod welded to the lower flange at midspan, and a yoke extension from the reference bars permitted readings outside the cooling chamber. Due to limited accessibility during the low-temperature tests, only midspan deflections were measured.

In order to compare the structural yield points, "refusal" loads and general load-deflection behavior of beams having different base metal properties and unknown effects of welding, it was desirable to maintain a constant loading rate. Due to interruption of loading for strain recording, inspection and necessary adjustments it was not possible to maintain constant loading conditions, particularly in the plastic range. It was possible, however, to reduce the observed load-deflection data to comparable curves at the same loading or deflection rates by the following procedure.

After the proportional limit was exceeded, as evidenced by the deflection increments, the loading which was applied at a convenient rate, was interrupted at various values of initially applied load and the deflection was maintained constant by readjustment of load by the testing machine as necessary. The increments of load or deflection between such interruptions were determined arbitrarily to give sufficient points for the derived curves. The load adjustments to maintain constant deflection—largely a machine effect—were small and intermittent. At each of the initially applied load increments, the decrease of load at constant deflection was recorded with time. Curves representing the decrease of load vs. time were called the "drop-off" or relaxation curves. At any interval of time after load application and stopping the machine, the instantaneous load was recorded as Pi. Then the rate of relaxation, dPi/dt was found from the relaxation curves at various values of Pi. A curve representing Pi vs. dPi/dt was plotted for each of the constant deflection increments. It was now possible to find values of Pi at selected relaxation

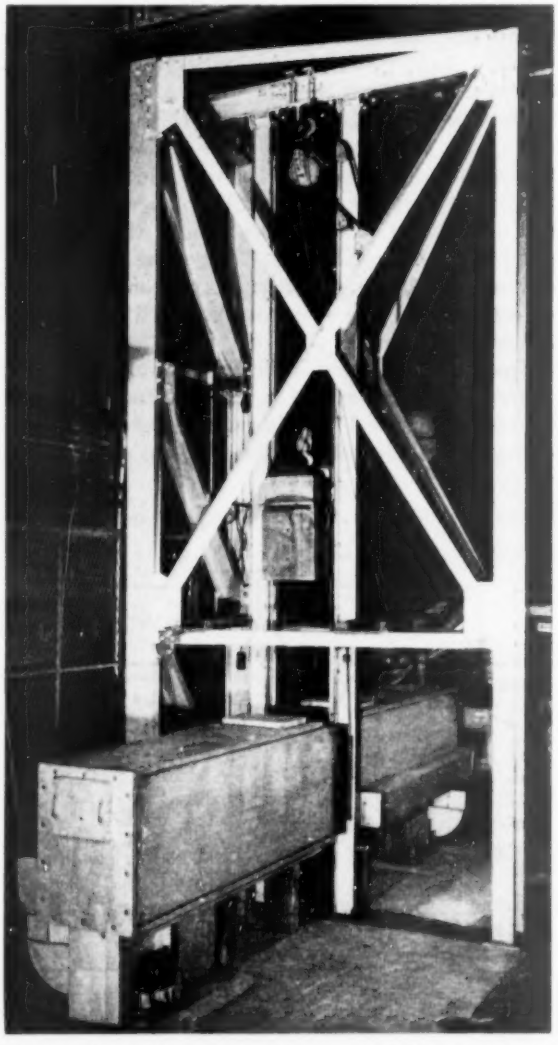


Plate 2 View of impact testing tower with beam specimen enclosed in cooling chamber

rates from each of the constant deflection curves. It will be noted that *Pi* represents the load resisted by the beam at a particular deflection when applied at the rate equal to the corresponding relaxation rate. Curves representing *Pi* vs. Δ can now be constructed for selected loading rates and the load-deflection data for the several beams can be compared with the same loading rate. The procedure is time consuming, if very low loading rates are used as the basis of comparison, because of the long period required for stabilization of the "drop-off" load. The highest rate is, of course, limited by the rate of initially applied load possible and controllable with the usual testing machine. However, the comparison can be made at relaxation rates which are reached in reasonably short holding times.

The effect of loading rate on a beam at room temperature and comparison of several welded beams at −115° F are discussed later. As noted previously, only a limited number of "static"—actually low straining rate—tests were made because the results were inconclusive in differentiating the steels and treatments with respect to brittle behavior.

Impact Tests

The major part of the test program consisted of increment drop impact tests. The tower structure providing guides and lifting facilities for the tup was described in the report of previous impact tests. Some modifications were made in the beam supports and instrumentation, and the entire tower was re-erected on a more massive concrete foundation, bearing on rock.

The striker weight or tup weighing 2000 lb consisted of a block of steel to the lower end of which was attached a $4^1/_2$- x $4^1/_2$- x $15^3/_4$-in. contact block of alloy steel with bottom rounded to a 3 in. radius. The tower was 20 ft high, providing a net height of fall for the tup of 9 ft.

The cooling chamber was the same as that used for the static tests with an opening in the top panel to permit passage of the falling tup for centrally applied impact. This opening was normally closed with a movable cover, opened only when the blow was applied. Figure 8 shows details of the test assembly and Plate 2 shows the tower and cooling chamber enclosing a beam mounted for test.

The heavy steel pedestals on which the beam was supported were anchored to the foundation. The beam-supporting fixtures were intended to provide, as nearly as possible, unrestrained "simple" beam conditions and prevent rebound. The upward forces developed at the supports, corresponding to the reactions on the beam when deflected upward during the vibration cycle, were of considerable magnitude and during the earlier tests caused rupture of fixture and anchor bolts. The fixtures used permitted one end to rotate but prevented sliding, while at the other end permitted longitudinal movement as well as rotation. The clamping details for these fixtures are shown in Fig. 9. While these fixtures were effective in preventing upward rebound at the supports, the dynamic deflection records indicate rapid damping of vibration probably due to unavoidable friction in the support fixtures.

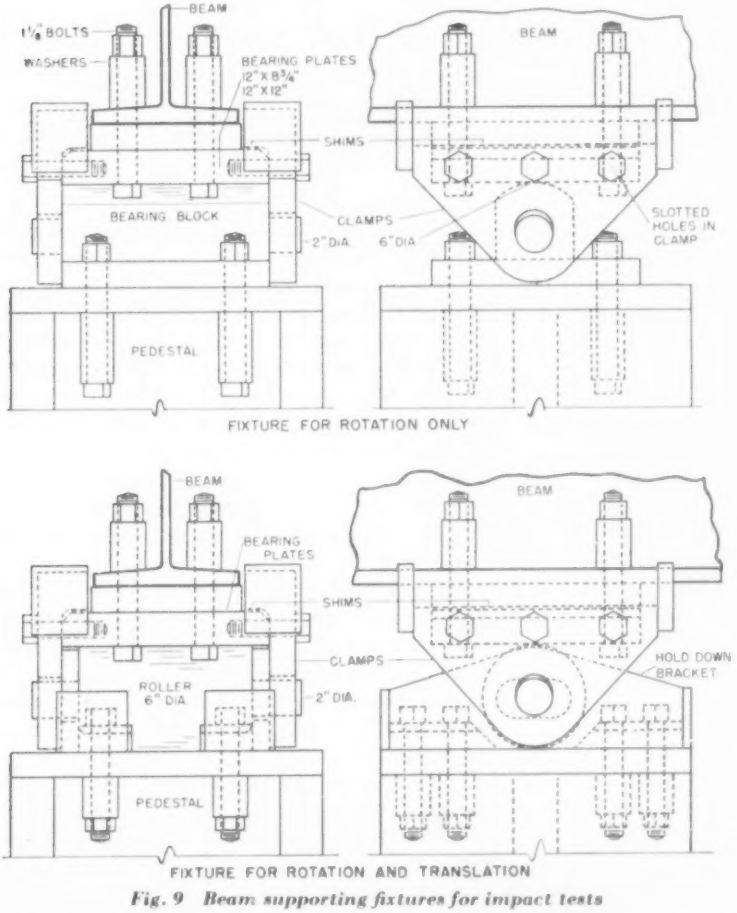


Fig. 9 Beam supporting fixtures for impact tests

All beams were subjected to central impact by the 2000-lb tup in drop increments of 3 in. up to a fall of 36 in. and thereafter in increments of 6 in. To minimize the effect of local contact pressures at the point of impact and avoid application of the load directly to the weld, a 3- x 6- x 12-in. contact block was attached to the upper flange at midspan. The top of this block was machined before each test to present an undented surface. In the earlier tests this block was held in place by two inverted angles welded transversely to the upper flange. The block was grooved to avoid bearing on the weld and the angles

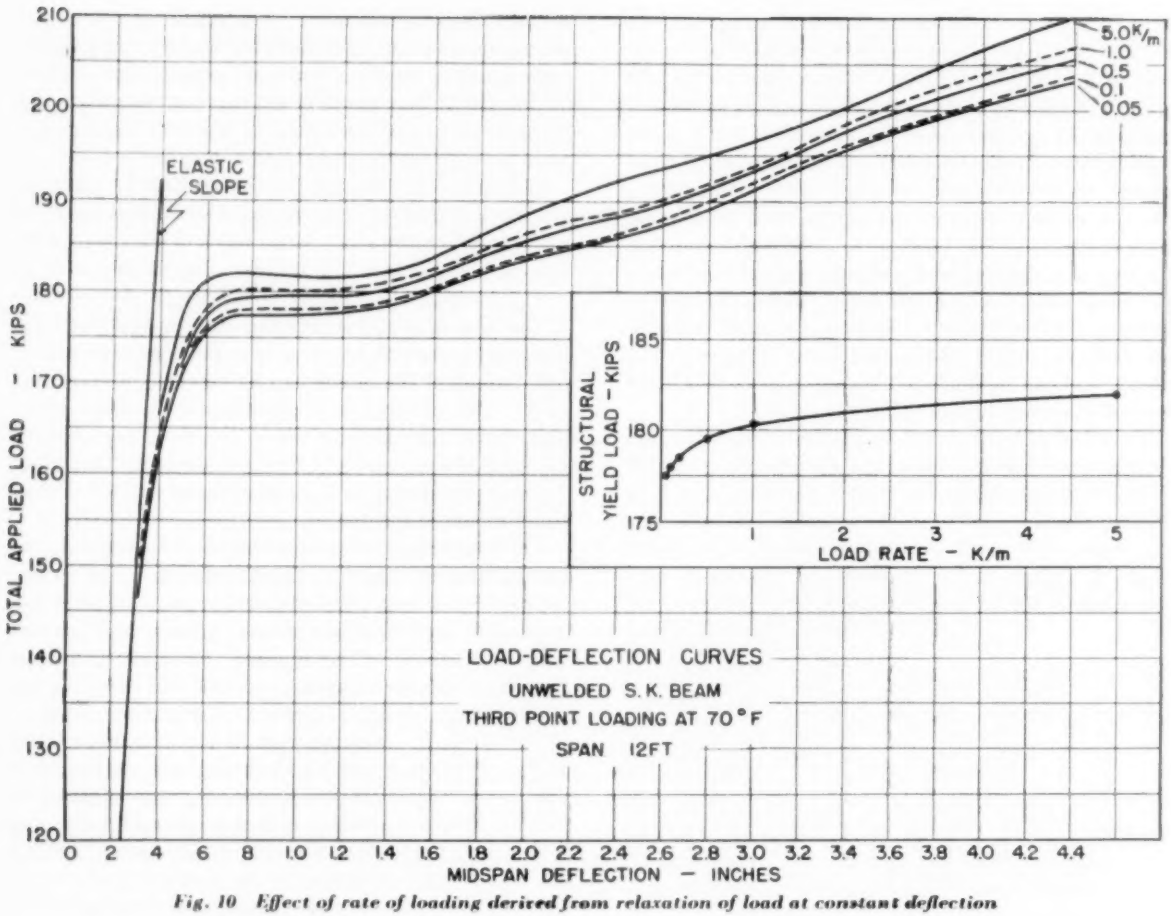


Fig. 10 Effect of rate of loading derived from relaxation of load at constant deflection

effectively held the block in place. Due to the flexibility of the angle legs, there was no appreciable flexure participation of the block with the beam section. Some of the complete fractures at low temperatures, starting at the lower flange extended through the upper flange adjacent to the bearing block close to the welds attaching the angles. Due to the possible influence of these angle welds in determining the location of the top flange fracture, the method of holding the contact block was changed to a simple bolted connection. Four bolts directly connected the block to the flange. While some fractures also occurred near the ends of the block, held by this method, no fractures occurred through the bolt holes.

In all tests, the beam either fractured or developed lateral buckling of the top flange with resulting pronounced over-all distortion of the section. The first tests made at −115° F were in the nature of pilot tests for comparison with the static tests made at the same temperature. Fractures resulted in beams of both steels and all treatments. Thereafter, tests were made at successively higher temperatures to determine the temperature above which the type of failure was due to normal beam buckling without cracks.

The midspan dynamic deflection was measured at all blows. The apparatus used for deflection measurement consisted essentially of an optical system in which a beam of light passing under the beam was directed to a photo-tube and partially intercepted by a cut-off plate attached to the beam. In order to extend the intercepting plate below the cooling chamber the plate was attached to the end of a rod welded to the bottom flange about $1^1/_2$ in. away from the splice on the beam centerline. The system is similar to that described for earlier tests, with an improved amplifier. The dynamic deflections were recorded on film by an oscillograph. The accuracy of the measurements was about ±0.005 in.

Dynamic strain measurements were made by SR-4 gages placed on the upper curved surface of the edge of the lower hole in the web and at points adjacent to the hole in the web and flange. Additional gages on the web weld measured permanent sets with a manual strain recorder. The equipment for dynamic measurements included David Taylor Model Basin strain indicators and a Hathaway oscillograph, adapted to the requirements of these tests. Four channels were available, recording strain transients and calibrations on a film record.

Strains were measured on beams of all types during

the earlier impact tests. In addition to determining the range of elastic action for comparison with the deflection measurements, the records served to indicate the start of cracks. When the behavior pattern was established, these strain records were discontinued and the proportional limit or structural yield strength was determined by the deflections.

In order to determine the amount of plastic deformation produced at critical locations adjacent to the fractures, scratch measurements were recorded for many of the beams. Prior to test, fine scratches spaced 0.1 in. apart were made over a length of 3 in. along a longitudinal line on the web immediately above the lower hole and also on the lower flange surface across the weld. The displacement of these scratches gave some indication of the amount of plastic deformation preceding or associated with the fracturing process.

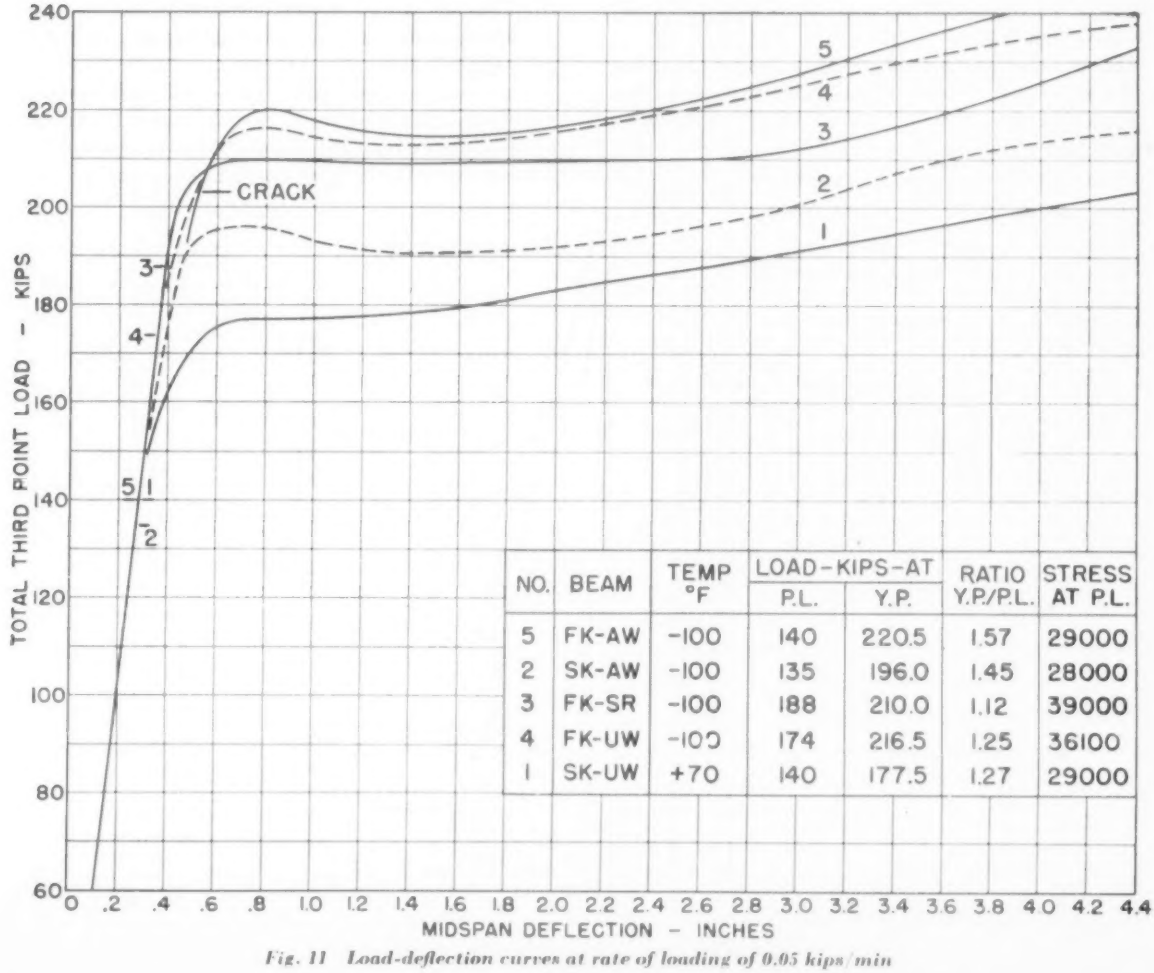
RESULTS OF STATIC TESTS

These pilot tests at a temperature of −115° F were intended to determine the advisability of further tests at varying temperatures. While the results did not differentiate the beams and treatments as regards the brittle fracture phenomena, they do afford some comparison of beam behavior of the two materials, the effect of loading rates and the effects of the web holes on the strains in the vicinity of the splice.

The effect of loading rate in these so-called "static" tests, which are really tests at low loading rates, is indicated by the curves shown in Fig. 10, derived from the relaxation of load at constant deflection. The range of loading rates from these data is not large and the spread of structural yield strengths amounts to about 2.5%. The increase of yield strength would be considerably greater for the higher loading rates possible with ordinary testing machines when the loads are applied by the usual continuous beam test procedures where deflection measurements are made "on the run." The relation between loading rate and the structural yield loads is shown by the insert curve of Fig. 10.

Comparative load-deflection curves derived from relaxation data are shown for several beams in Fig. 11. The as-welded beams, both SK and FK,* have low ap-

* Reference to semikilled and fully killed steel will hereafter be abbreviated by SK and FK, respectively.



NO.	BEAM	TEMP °F	LOAD-KIPS-AT P.L.	LOAD-KIPS-AT Y.P.	RATIO Y.P./P.L.	STRESS AT P.L.
5	FK-AW	-100	140	220.5	1.57	29000
2	SK-AW	-100	135	196.0	1.45	28000
3	FK-SR	-100	188	210.0	1.12	39000
4	FK-UW	-100	174	216.5	1.25	36100
1	SK-UW	+70	140	177.5	1.27	29000

Fig. 11 Load-deflection curves at rate of loading of 0.05 kips/min

parent proportional limits due to residual stresses. The ratio of structural yield point to proportional limit is theoretically 1.15 for this section. The curves give values of 1.45 and 1.57 due to the lower PL. The FK, stress-relieved beam gave a ratio of 1.12 which is nearly the theoretical value indicating removal of residual stresses since the properties are not appreciably effected by this treatment. The unwelded FK beam at −100° F and SK beam at +70° F also showed somewhat high YP/PL ratios, probably due to rolling residuals. These limited tests give some indication of the beam properties, reflecting material properties and effect of temperature, although data are not available over a range of temperatures because this series of tests was discontinued.

All of the beams tested statically developed "refusal" loads by lateral buckling of the compression flange. One as-welded, FK beam tested at −100° F (Curve *5*) cracked suddenly in the web at a deflection of 0.53 in. and at a load below the structural yield strength. This crack, starting at the end of the web weld at the top of the lower coped hole, extended to mid-height of the web (neutral axis). It did not extend beyond its initial formation with subsequent loading to buckling. Strain measurements indicated that appreciable permanent deformations in the web adjacent to the hole developed prior to the crack.

The partial fracture of the web with flanges remaining intact was found by later impact tests to represent an intermediate type of failure between buckling, with no cracks, and complete shattering of the entire section; that is, a tendency of crack propagation in the transition range. The formation of a brittle crack suggests therefore that the test temperature was in the brittle range. However, the unexpected fracture of a FK beam rather than a SK beam having less favorable properties, at the same temperature, is contrary to the findings of the impact tests and its significance is questioned. The relative tendency to brittle behavior was not established by these static tests.

Strain Analysis

Extended surveys of the strain distribution in the vicinity of the splice were made on the beams tested statically. The purpose of these strain surveys was to determine the influence of the holes in the web adjacent to the flanges, particularly in the plastic range. The findings are of particular interest as regards the origin of fractures produced in the impact tests.

The measurements extended over a length of 32 in. in the segment of constant bending moment between third-point loads. Longitudinal strains were measured with SR-4 electric gages placed on the flanges and web so as to determine the distribution over the beam section at the weld, 1¼ in. from the weld and at sections remote from the splice. Additional gages were placed to find the lateral distribution on the outer flange surfaces and the variation along the flanges.

The results obtained on beams of both materials were in substantial agreement with respect to the influence of the holes. The data presented represent the performance of a SK beam, as-welded, tested at −100°

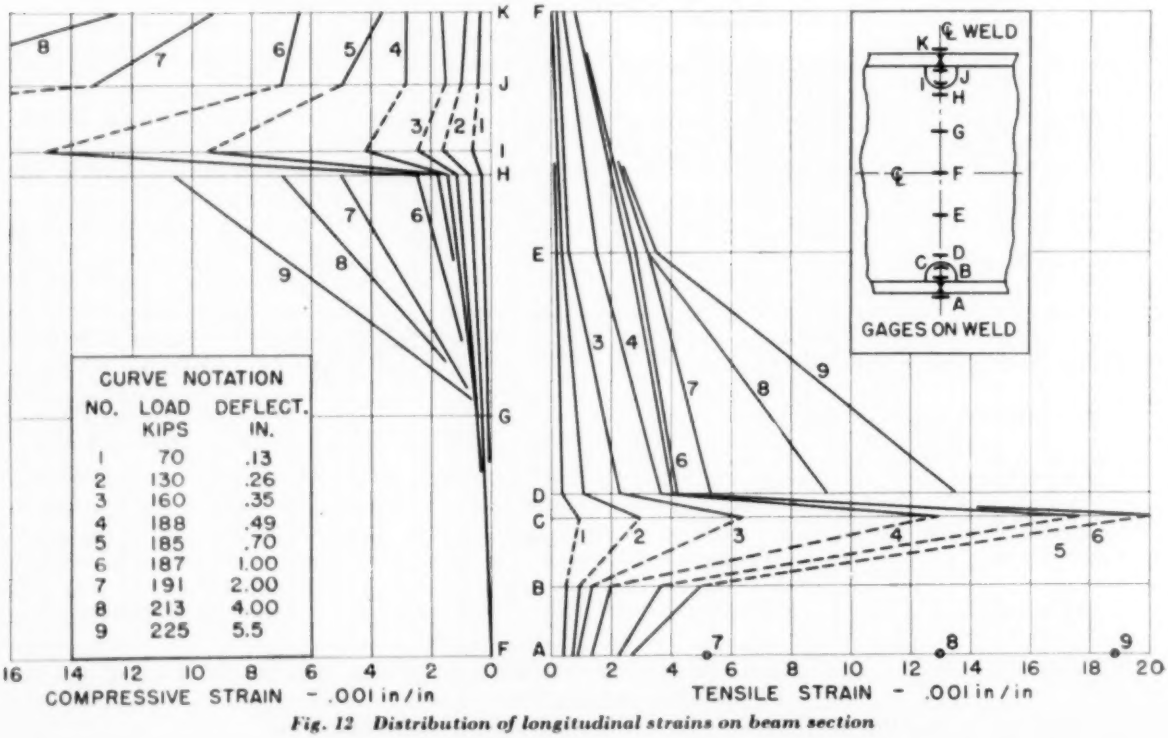


Fig. 12 Distribution of longitudinal strains on beam section

F. The distribution of longitudinal strains measured on the welded splice section, with weld reinforcement ground flat, is shown in Fig. 12. Gage stations *A* and *K* were on the tension and compression flange surfaces, respectively, directly opposite the web. Gages *B* and *J* were on the inner surfaces of the flanges, in the web hole. Gages *C* and *I* were on the curved edge of the hole, while *D* and *H* were on the web, $^1/_4$ in. from the edge of the hole. Gages *E*, *F* (centerline) and *G* were on the web surface. The curves plotted were for the deflection increments and total loads, applied at the third points of the span, as shown. Plotted values represent the average of two gages on opposite sides of the web and the average of two adjacent gages on the outer flange surfaces.

The influence of the holes in the web is indicated by the nonlinear distribution and the large concentrations of strain at the curved edges of the holes. This concentration is apparent in the elastic loading range with a factor of about 2 and increases rapidly with plastic deformation. The distribution is similar in both the tension and compression regions. Tension gages were lost at about 2.0% strain but, on the compression side, strains of 4.6% were measured at a beam deflection of 5.5 in. when the test was discontinued. The neutral axis moved toward the compression flange with large plastic deformations. A similar strain analysis was made at a section 8 in. from the weld splice where no hole existed. The distribution was found to be more nearly linear but with a tendency for the tensile strains at the level corresponding to the top of the hole, to lag under the large plastic deformations, probably reflecting the large strains developed at the neighboring section due to the hole.

The distribution of longitudinal strains along the centerlines of both flanges was measured over a distance extending 16 in. each side of the splice. Pairs of gages were placed on the weld, $1^1/_4$ in. from the weld and at more remote distances. The curves shown in Fig. 13 indicate the start of yielding and rapid increase of plastic strain in the base metal $1^1/_4$ in. from the center of the weld. The unsymmetrical appearance of these curves in the vicinity of the weld is due to the lack of corresponding measurements adjacent to, but on the other side of, the weld, where the curves are shown dotted. With increasing deformation there is a decided strain lag in the weld metal; that is, the weld metal is stiffer.

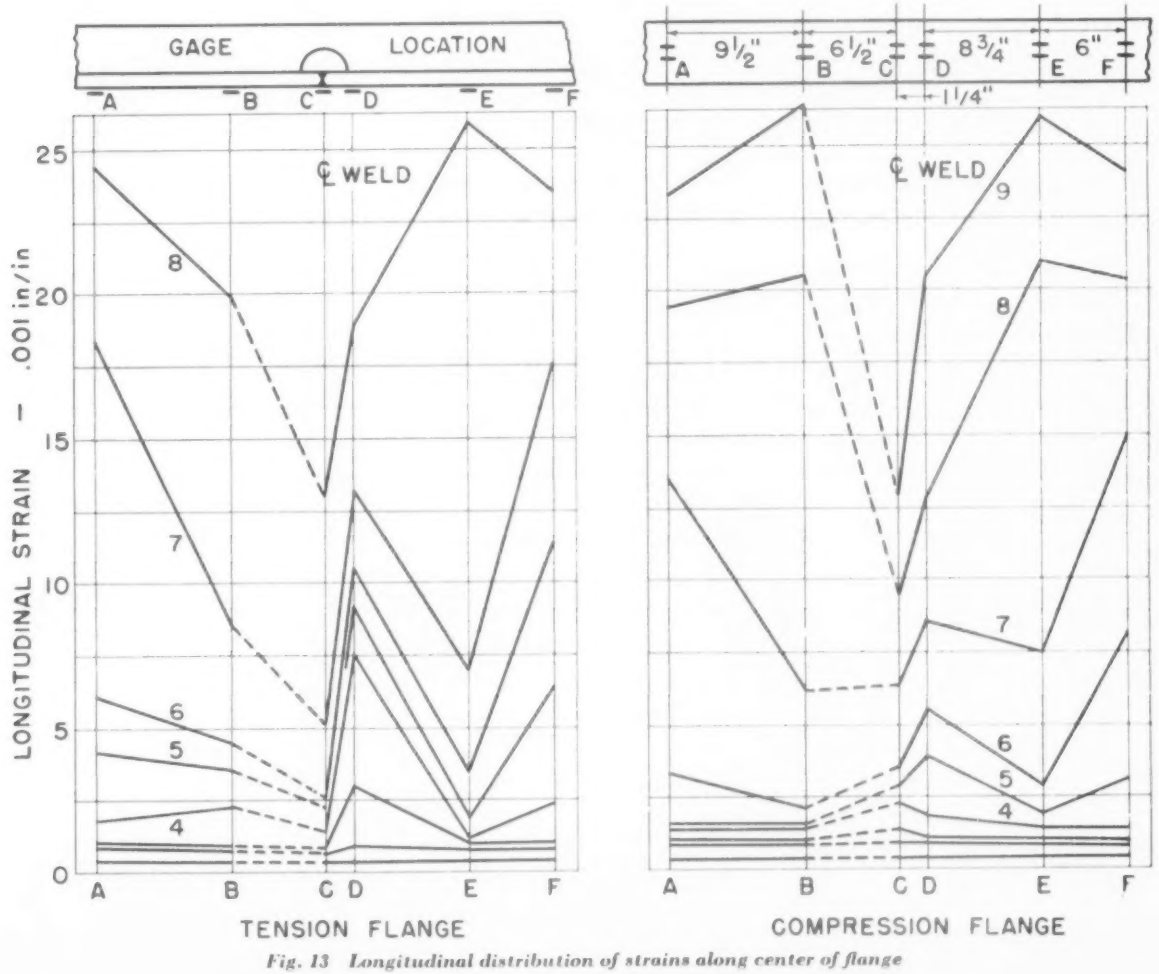


Fig. 13 Longitudinal distribution of strains along center of flange

The transverse distribution of strain on the flange surfaces is shown in Figs. 14 and 15. While the elastic strains are uniform across the flange, yielding progresses much more rapidly under the web. The pair of gages were opposite the web fillets and strains may have been larger on the centerline. The corresponding load curves indicate larger strains at the stations $1^1/_4$ in. from the weld. Yielding also progressed more rapidly under the web at a section 10 in. from the weld indicating normal behavior of the rolled section in which the strain at the edges of the flange tend to lag particularly on the tension flange. Buckling of the compression

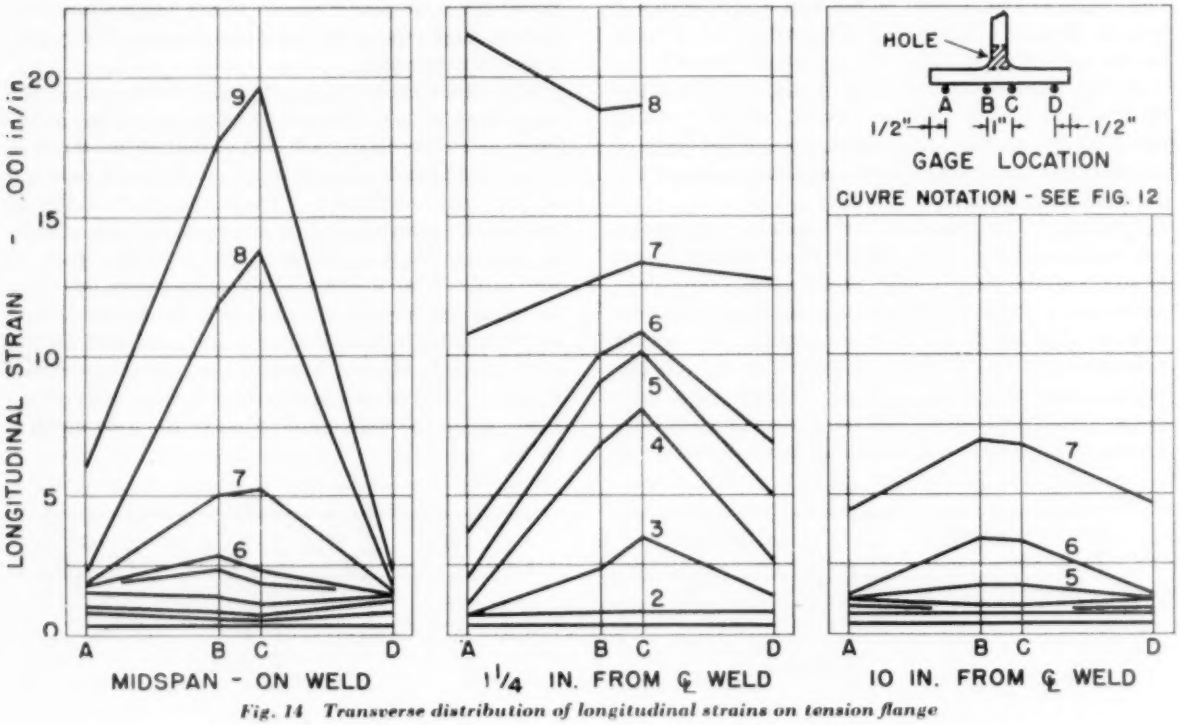


Fig. 14 Transverse distribution of longitudinal strains on tension flange

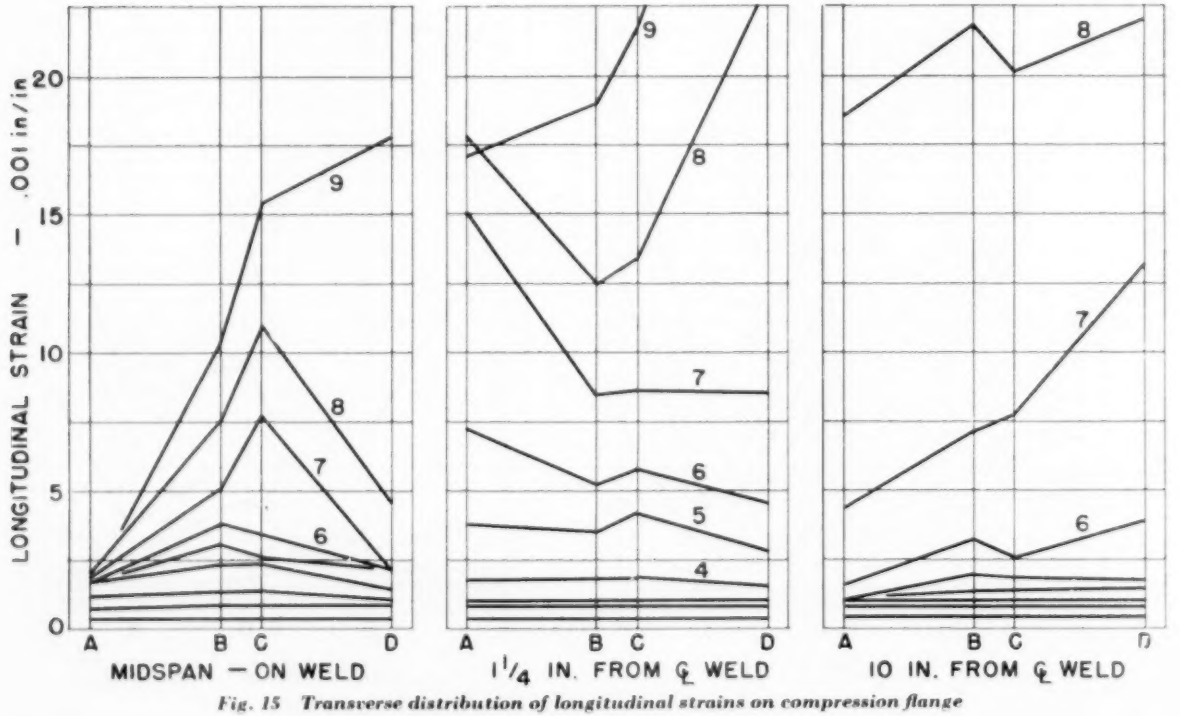


Fig. 15 Transverse distribution of longitudinal strains on compression flange

flange tends to increase the edge strains.

As will be discussed later, an estimate of the strain rate produced by dynamic loading of these beams is complicated by the wide variation of local strains. For a particular deflection rate, the strain rate would be greatest at points of greatest strain. The origin of fractures produced in the impact tests confirm the results of these strain analyses.

RESULTS OF IMPACT TESTS

The experimental data recorded in these tests of centrally applied impact by successively increasing drops of a 2000-lb tup included dynamic deflection and strains, independent dial gage measurements of permanent deflection set at each blow, permanent deformations by scratch displacement measurements after test and crack propagation observations. Most of the derived relationships were based on the deflection data. The strain records made on the earlier beams tested confirmed the elastic range found from the load-deflection relations and the initiation of cracks. These strain measurements were discontinued in the later tests. A summary of test results and derived data is given in Table 5 which includes supplementary tests to be discussed later. This summary includes the height of fall at which fracture was produced or the height when tests were discontinued because of pronounced distor-

Table 5—Results of Impact Tests on Butt-Welded Beams

Mark	Temperature, °F	Height of fall, in.*	Strain energy, ft-kips,†	Deflection, in.,‡	Equivalent loads, kips, PL	Equivalent loads, kips, YP§	Ratio YP/PL	Stresses, ksi, PL	Stresses, ksi, YP‖	Failure
Semikilled steel, as-welded, E6011-20 electrodes										
1E1	−114	60	7.2	0.55	...	...	...	...	...	Complete fracture
4C	−40	66–78	10.8–18.7	0.79–1.18	222	266.5	1.20	69.0	82.7	Web fracture to complete
4C1	−30	90–108	24.6–50.6	1.45–2.85	210	262	1.25	65.2	81.4	Web fracture to buckle
5C	−20	96–108	31.3–42.8	1.80–2.42	199.5	254	1.275	62.0	78.9	Web fracture to buckle
2D	−10	108	56.5	3.14	195	243	1.245	60.5	75.5	Buckled
1D1	−10	109	52.3	2.74	195	254	1.30	60.5	78.9	Buckled
4A	0	90	31.6	1.61	180	229	1.27	55.9	71.1	Web and lower flange fracture
1D	+5	84	27.3	1.72	175	232	1.325	54.3	72.0	Buckled
2E	+10	102	41.2	2.53	170	229	1.345	52.8	71.1	Buckled
4A1	+20	108	48.7	2.97	180	226.5	1.26	55.9	70.4	Buckled (small crack)
						Avg	1.275			
Semikilled steel, welded and stress-relieved E6011-20 electrodes										
5E1	−118	60	7.6	0.60	...	...	...	...	...	Complete fracture
3E1	−40	72	12.7	0.92	235	258	1.10	73.0	80.1	Web and lower flange fracture
2D1	−25	78	19.0	1.17	213	253	1.19	66.1	78.5	Complete fracture
4B1	−15	60	8.8	0.63	225	250	1.11	70	77.6	Web and lower flange fracture
3D	−10	102	32.4	1.94	200	249.5	1.245	62.1	77.5	Buckled
4B	−5	60–72	8.5–17.3	0.70–0.94	204.5	242	1.182	63.5	75.2	Web fracture to complete
5A	0	78–84	20.6–23.8	1.32–1.48	205	243	1.185	63.6	75.5	Web and lower flange fracture
5B1	+5	96	29.8	1.84	196	241	1.23	60.9	74.9	Buckled
3D1	+15	90	29.0	1.79	185	236	1.275	57.4	73.3	Buckled
5E	+30	110	53.6	3.63	180	215	1.19	55.9	66.8	Buckled
						Avg	1.19			
Semikilled steel, preheat and welded, E6011-20 electrodes										
3E	−115	33	4.1	0.47	...	...	...	...	...	Complete fracture
4D	−40	72	13.4	0.92	210	...	...	65.2	...	Web and lower flange fracture
										All outside weld zone
3A	−39	107	43.1	2.20	220	270	1.23	68.4	83.8	Complete fracture
2E1	−25	54–66	8.4–11.5	0.69–1.14	208	244	1.17	64.6	75.7	Web and lower flange fracture
4D1	−20	30	3.8	0.47	...	...	...	...	...	Lower flange only fracture
5D1	−15	108	40.6	2.33	195	248	1.27	60.5	77.0	Buckled
5A1	−10	108	43.2	2.49	195	246	1.26	60.5	76.4	Buckled
5D	−5	90	27.1	1.69	190	234.5	1.24	59.0	72.8	Buckled
						Avg	1.23			
Fully killed steel, as-welded, E6011-20 electrodes										
8A	−120	84	14.6	0.90	209	295	1.41	64.9	91.6	Complete fracture
4D1	−60	48–60	6.4–9.4	0.59–0.75	212	...	...	65.8	...	Web fracture to complete
6D	−50	84–90	21.6–34.0	1.33–1.96	220	252.5	1.15	68.3	78.4	Web and lower flange fracture
1B1	−50	108	36.0	1.70	220	259.5	1.18	68.3	80.5	Buckled
7E	−40	96–108	39.0–46.3	2.21–2.63	179	250.5	1.40	55.6	77.8	Web fracture only
6D1	−35	60–78	9.5–17.0	75–1.16	217	...	...	67.4	...	Web fracture to complete
8A1	−32	96–102	35.0–40.3	2.07–2.36	195	240	1.23	60.5	74.5	Web fracture only
4D	−26	90	24.7	1.50	207	247	1.19	64.3	76.7	Buckled
8E1	−20	102	41.6	2.50	170	239.5	1.41	52.8	74.4	Buckled
1B	−10	84	22.6	1.47	179	234.0	1.31	55.6	72.6	Buckled
						Avg	1.29			

tions caused by buckling. Where two values of height of fall, deflection and strain energy are shown, the first represents the blow producing first crack and the second represents a major extension of this crack in either the flange or web, or the value at which the test was discontinued due to buckling of the beam. The maximum height of fall available was 108 to 110 in.

Equivalent Static Loads

The vibration and subimpact phenomena of centrally applied impact was discussed in a previous report.[1] The elastic dynamic deflection can be predicted very closely by the approximate formula for the equivalent static load producing the same center deflection, with

Table 5—Results of Impact Tests on Butt-Welded Beams (*Continued*)

Mark	*Temperature, ° F*	*Height of fall, in.**	*Strain energy, ft-kips,†*	*Deflection in.,‡*	*Equivalent loads, kips PL*	*YP§*	*Ratio, YP/PL*	*Stresses, ksi PL*	*YP‖*	*Failure*
Fully killed steel, welded and stress-relieved, E6011–20 electrodes										
7E1	−117	72–78	9.8–11.5	0.72–1.06	312	...	...	96.9	...	Web and lower flange fracture
5A	−60	96–108	30.2–47.0	1.60–2.27	255	276	1.08	79.2	85.7	Web and lower flange fracture
2B1	−55	15	1.9	0.32	...	...	...	..	...	Web and lower flange fracture
4E1	−50	54–96	9.1–43.4	0.71–2.38	240	279	1.16	74.5	86.6	Web and lower flange fracture
5A1	−40	96	33.2	1.76	230	250	1.09	71.4	77.6	Buckled
2B	−30	96	33.1	1.92	220	242	1.10	68.3	75.1	Buckled
4E	−20	96	39.6	2.16	215	243.5	1.13	66.8	75.6	Buckled
						Avg	1.14			
Fully killed steel, preheat and welded, E6011–20 electrodes										
8E	−103	18	2.3	0.35	...	...	...	..	...	Complete fracture
6A1	−80	96	22.6	1.20	240	278.5	1.16	74.5	86.5	Complete fracture
6A	−70	54–72	6.8–14.8	0.60–1.02	236	245	1.04	73.3	76.1	Web and lower flange fracture
5B1	−65	36	4.3	0.49	...	...	...	..	...	Web and lower flange fracture
5E	−60	108	24.0	1.38	225	275	1.21	69.9	85.4	Buckled
5B	−55	54	7.2	0.63	...	...	...	..	...	Complete fracture
3B1	−50	96	27.4	1.60	209	255	1.22	64.9	79.1	Buckled
3B	−40	102	32.2	1.88	200	252	1.26	62.1	78.2	Buckled
						Avg	1.18			
Semikilled steel, unwelded, flame-cut holes in web										
2C	−115	48	5.7	0.56	...	...	...	...	...	Complete fracture
3C1	−60	108	34.05	1.84	200	276.5	1.38	62.1	85.9	Buckled
1C1	−40	96	27.3	1.61	190	259.5	1.37	59.0	80.5	Buckled
1C	−20	90	27.8	1.66	185	246.5	1.33	57.4	76.5	Buckled
						Avg	1.36			
Fully killed steel, unwelded, flame-cut holes in web										
4B	−115	107	23.2	1.20	200	307	1.53	62.1	95.4	Complete fracture
6B	−60	84	20.4	1.42	185	254	1.37	57.4	78.9	Lower flange only fractured
8B1	−40	66	13.0	0.81	160	254.5	1.59	49.6	79.0	Lower flange only fractured
6B1	−20	90	21.7	1.44	140	233	1.66	43.5	72.4	Buckled
						Avg	1.54			
Semikilled steel, unwelded but stress relieved, drilled holes										
4E	−115	107	...	..	305	325	1.07	94.6	101.0	Buckled
Semikilled steel, unwelded, drilled holes, pressed notch										
2C1	−60	48	6.4	0.60	225	...	...	70.0	...	Web crack at notch Lip = 0 to trace
3B1	−30	42	5.3	0.53	215	...	...	66.7	...	Web crack at notch Lip = 0.01 in.
3B	+30	42	6.2	0.60	165	...	...	51.2	...	Web crack at notch Lip = 0.02 in.
Fully killed steel, unwelded, drilled holes, pressed notch										
2D1	−30	54	7.5	0.69	200	...	...	62.1	...	Crack at 54-in. drop, discontinued Lip = 0.015 in.
3D	+30	48–84	7.6–26.7	0.66–1.75	168	221.5	1.32	52.2	68.8	Crack at 48-in. drop Buckled at 84-in. drop
2D	+63	54–84	11.7–32.4	0.91	163	208.5	1.28	50.6	64.8	Crack started at 54-in. drop Buckled at 84-in. drop Lip = 0.10 in. (tear)

* Height of fall producing first crack or when discontinued due to buckle. Second value is height of additional increments to subsequent extended fracture or buckle as indicated.
† Total strain energy accumulated to first crack. Second value represents energy to subsequent buckle or extended fracture.
‡ Total deflection to first crack, subsequent buckle or fracture extension.
§ Equivalent static load producing elastic component of observed dynamic deflection.
‖ Computed by flexure formula.

the appropriate inertia correction factor.* The elastic deflection predicted from this equivalent static load agreed with actual dynamic observations within less than 2%. It was also found that the observed dynamic deflections, prior to buckling of the beam, varied linearly with the striking velocity of the tup, as predicted by the equivalent static load formula, where P_{eq} is proportional to $\sqrt{h}$. The primary assumptions made in deriving this approximate formula are (1) that the contact losses are negligible and (2) that the elastic deflection curves at maximum deflection are the same when loaded statically or dynamically. That the elastic curves are practically the same was shown by measurements of deflection along the beam as previously reported.[1] The relatively small indentations produced in the bearing block struck by the tup and the rigidity of the supports justifies the assumption of small energy losses, prior to buckling.

In the plastic range, the equivalent static load was found from the elastic component of the dynamic deflection which was represented by the deflection recovery after the blow. Curves representing the equivalent static load plotted against the total deflection, including the dynamic deflection produced by the blow and the previous permanent set, were similar to the familiar static load-deflection curves as shown in Figs. 10 and 11. From these curves, the loads corresponding to the flexural proportional limit and the structural yield point (knee of curve) were determined and the corresponding stresses computed by the flexure formula. The proportional limit stresses thus found agree very closely with the indicated start of yield as measured by electrical strain gages.

* $P_{eq} = \sqrt{\frac{96\,EI\,W_t}{L^3}} \times C = 36.2^k \sqrt{h''}$ approximately where C = inertia correction $= \frac{1 + \frac{17}{35}\frac{W_b}{W_t}}{\left(1 + \frac{5}{8}\frac{W_b}{W_t}\right)^2} = 0.72$ for W_b = weight of beam (1000 lb), W_t = weight of tup (2000 lb).

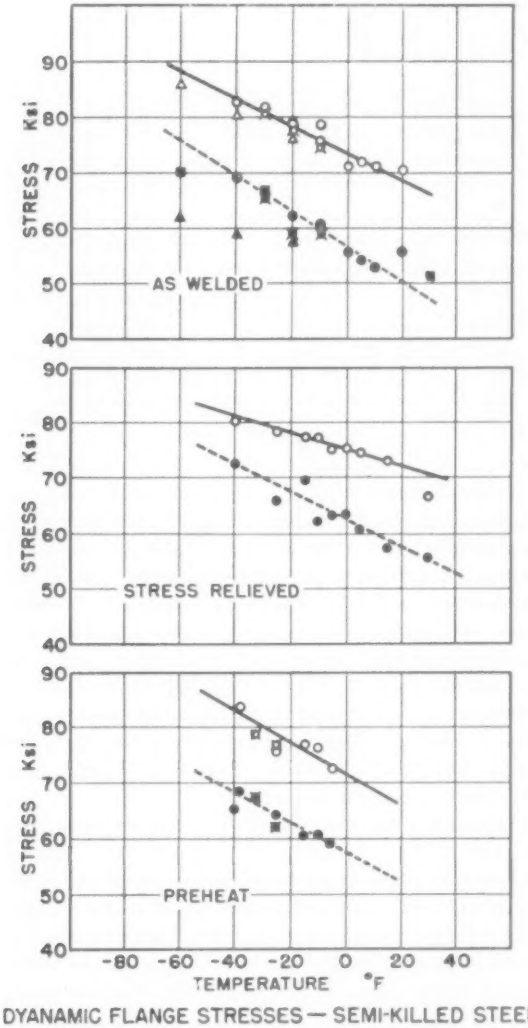


Figure 16

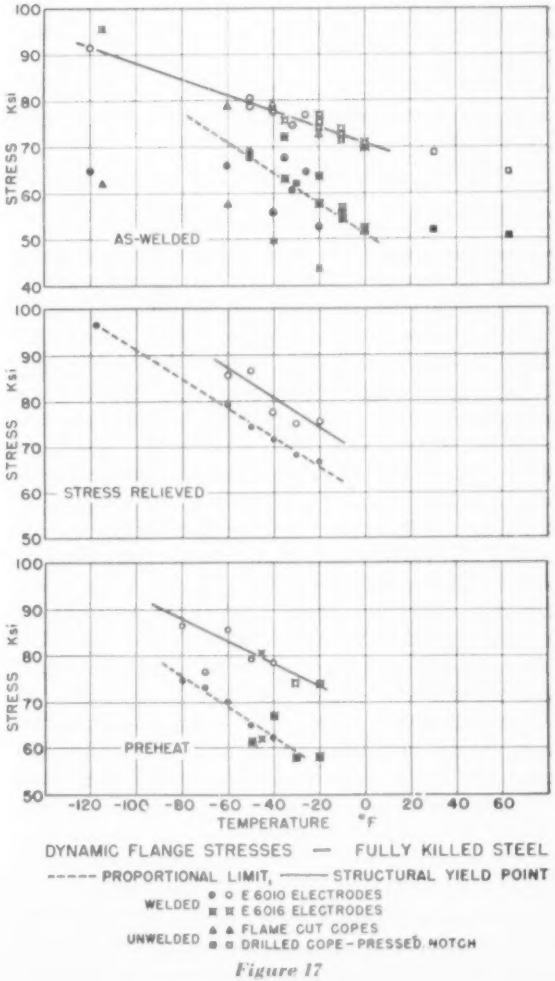


Figure 17

The dynamic flange stresses for both steels and each of the welding treatments, and their relation to the test temperature, are shown in Figs. 16 and 17. The variations of stress with temperature are shown as straight lines over the range of test temperatures although flat curves might be expected. Both the proportional limit and yield point stresses increase with decrease of temperature. The higher values as compared with the tensile properties of the base metal are due to both temperature and rate of loading. The ratio of the structural yield point to the proportional limit is given in Table 5. The theoretical ratio for this beam section is 1.15. It will be noted that the as-welded beams of both steels show ratios of about 1.28 due to the lowering of the apparent proportional limit by residual welding stresses. When stress relieved, the ratios were 1.19 and 1.14 for the SK and FK beams, respectively, which agrees substantially with the theoretical value of 1.15, indicating that the residual stresses were effectively relieved. The preheated beams show somewhat less residual stress effect by those ratios than the as-welded beams.

Although tests were made on all types of beams at -110 to $-120°$ F, stress values are lacking in most of these diagrams because failure occurred by shattering fracture without developing any permanent deflection set; i.e., below the proportional limit. The elastic behavior of the beams fabricated with low-hydrogen electrodes is essentially the same as those welded with E6020 electrodes.

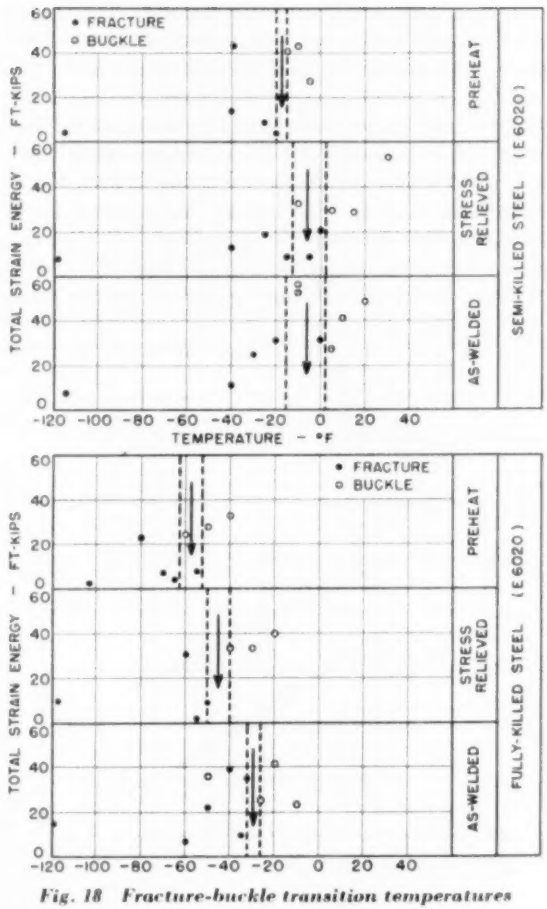


Fig. 18 Fracture-buckle transition temperatures

Strain Energy

The strain energy stored in the beam by the dynamic load is derived from the kinetic energy of the tup. However, part of the energy of the falling weight is expended in accelerating the beam so that theoretically the flexural strain energy represents 72% of the energy of the striking weight, for the weights of beam and tup used in these tests. This is the inertia correction factor included in the equivalent load formula. Additional energy losses are involved. Although contact losses can be neglected, a very considerable decrease in the energy available for flexure of the beam in the plane of loading may result when buckling of the beam develops because of the lateral deflection and twisting produced and the energy transmitted to the guides when the tup is deflected laterally by the distorted beam. The flexural strain energy may then represent only 50 to 60% of the kinetic energy of the tup.

The flexural strain energy was computed from the area under the equivalent static load-deflection curves, which are based on the observed dynamic deflections and permanent sets; that is, the actual beam performance. These strain energy values at first crack or buckle are shown in Table 5. The strain energy values to fracture or buckle for beams of both steels and each of the treatments when welded with E6020 electrodes, at the test temperatures are shown in Fig. 18.

The accumulated strain energy stored in the beam under successive blows as computed may be affected by strain aging. However, for the beams tested at temperatures below the transition range, the plastic deformations at fracture were small and because of the low temperature and short time intervals between blows, this possible aging effect is believed to be unimportant for the comparisons made here. Also, at the higher temperatures when buckling with considerable plastic deformation was involved, the exact energy value is not significant because the total energy is largely dependent upon the progress of buckling and when the test was discontinued.

In general, when fractures develop, the energy values are low. When buckling determines the ultimate behavior, the strain energy is considerably larger. Had the beams been braced laterally, much greater deflections could have developed with corresponding increase of plastic energy and the energy values would have shown a more abrupt drop in the fracture range. The points in Fig. 18 representing strain energy for beams in which the test was discontinued due to excessive buckling—namely, at the higher temperatures—are erratic and too low. Using the beam deflections rather then strain energy would not greatly change the comparison of behavior since both the deflection and

strain energy reflect the amount of plastic deformation developed.

Transition Temperatures

Figure 18 shows the temperatures at which a transition in type of failure from buckling to fracture occurs. This temperature is taken as the mean of a range of uncertainty within which there is a probability of either type of failure, according to the number of tests made. Based on this buckle vs. fracture criterion, transition temperatures are fairly well defined, as shown in Table 6.

Table 6—Transition Temperatures, ° F, for Butt-Welded Beams, Buckle vs. Fracture Criterion

Weld treatment, E6011-20 electrodes	*Type of steel*	
	Fully killed	*Semikilled*
As-welded	−29	−5
Welded-stress relieved	−45	−5
Welded with preheat	−57	−17

The results show lower transition temperatures for the welded fully killed steel beams by 24° F in the as-welded condition and 40° F for both the stress relieved and preheat treatments. While both steels show more favorable transition temperatures when welded with preheat, the effect is less marked in the case of the semikilled steel, which also shows no improvement by stress-relieving treatment.

The two grades of steel selected for these tests had appreciable differences in low-temperature notch sensitivity. In both the Charpy and Kahn tests the FK steel showed lower base metal transition temperatures. The beam weldments also show more favorable transition temperatures for the FK steel and therefore seem to reflect, qualitatively, the base metal properties. More correctly, it is probable that both the notch sensitivity and weldability as indicated by the performance of the weldments, reflect the same inherent property that determines transition temperature and crack propagation. The variation of transition temperatures determined by the Charpy and beam tests, due to specimen location, arbitrary criteria and welding treatments, does not permit a quantitative correlation. The relation of Charpy values to crack propagation will be discussed later.

FRACTURES

The origin of the fractures was influenced by the weld and the geometry of the specimen; that is, the presence of holes in the web. The configuration of the fracture was also influenced to some degree by the method of loading. It has been shown that a large strain concentration was produced in the web at the inner edges of the holes. Likewise, the strains in the flanges were greatest adjacent to the weld, opposite the edges of the web holes. The strain rates under dynamic loads were also greatest at these points. The holes were formed by manual flame cutting. There were some gouge marks from the oxy-acetylene cutting on all specimens. The location of these holes immediately adjacent to the flange made it difficult to shape the holes and cut a flat flange surface because the torch had to be held so close to the flange. Before testing, the top edge of the hole and the surface on the flange on the tension side of all beams were smoothed with a file. This filing was done on the earlier beams tested to permit placement of electric strain gages on the inside curved surface and on the flat surface of the flange in the hole. Usually, this filing removed only the scalloped irregularities of flame cutting. Deep grooves could not be removed, particularly in the corners of the semicircular hole. This practice was continued when gages were not used, to maintain uniformity of conditions. While the gouging grooves are strain raisers, the root radii are relatively large and it was found that fractures frequently originated at other locations with smooth surfaces in spite of the existence of grooves which might be expected to invite the start of cracks. The surface irregularities themselves were probably not critical to the relative behavior of the beams. However, the formation of shallow thermal cracks and the modified structure at the flame-cut edges may have influenced the origin of fracture. Supplementary tests on unwelded beams with flame-pierced holes were made and are discussed later.

The beams were first tested at −110 to −120° F, at which temperatures complete fractures were produced. Tests were then made at increasing temperatures until failure resulted by buckling without fracture. In order to locate the transition temperature with a minimum of specimens, the tests were concentrated around the transition range. The extent and configuration of the fractures were related to the testing temperature, propagating further across the section and at lower deflections as the temperature decreased.

At the higher temperatures above the transition range, the beams buckled by sidewise deflection of the unbraced upper flange and twisting of the section. Very little local crippling of the web resulted under the loading block although in a previous investigation where beams were tested at room temperature, pronounced dishing of the web and local depression of the loaded flange was produced. The buckling of the beam as a whole without pronounced local distortions may be due to the heavier tup used in these tests. The maximum vertical deflection produced depended on the progress of buckling which determined when the test was discontinued. The large distortions demonstrated the lack of brittleness at these higher temperatures.

A photographic record was made of all beam fractures. The views shown in Plates 3 to 15 are intended to illustrate typical fractures in a sequence of propagation without regard to the particular steel or treatment but are identified for reference. A complete record of the type of fracture is given in Table 5.

The first visible sign of distress in the splice as the test temperature decreased was the formation of a "dimple"—i.e., pronounced contraction of thickness—

Plate 3 SK beam 5.41 welded with preheat, tested at −10° F, buckled with deflection of 2.49 in. Web weld directly over hole "dimpled" with large deformation, no cracks

in the web immediately above the hole on the tension side. This local deformation was in the weld metal but at a point of maximum strain concentration (see Plate 3). No crack developed and the beam resisted dynamic load to buckling. The first cracks produced occurred at the top edge of the lower hole. As shown in Plate 4, these cracks, actually tears, extended $^1/_2$ to $^3/_4$ in. in the weld metal and did not propagate with subsequent blows. Some of these partial web cracks also extended to mid-height of the web, Plate 5, in or adjacent to the weld up to the neutral axis and then entered the base metal, sometimes terminating in a "dimple" or local contraction where the energy was evidently dissipated by plastic deformation. The web cracks tended to branch and sometimes extended to the upper flange fillets. From the scale patterns on the web surface, this branching crack seemed to be avoiding the local compression zone under the loading block. However, the branching occurred at different heights

Plate 4 SK beam 4.41 tested as-welded at +20° F. Tear in weld at top of hole. Beam buckled with deflection of 2.97 in. with no propagation of crack

Plate 5 FK beam 7E tested as-welded at −40° F. Web crack started in weld at deflection of 2.21 in. and extended as shown at deflection of 2.63 in. Crack terminates in base metal with "dimple" at end

Plate 6 FK beam 6D tested as-welded at −50° F. Web crack started in weld at deflection of 1.33 in. Web crack extended and flange fractured at 1.96 in.

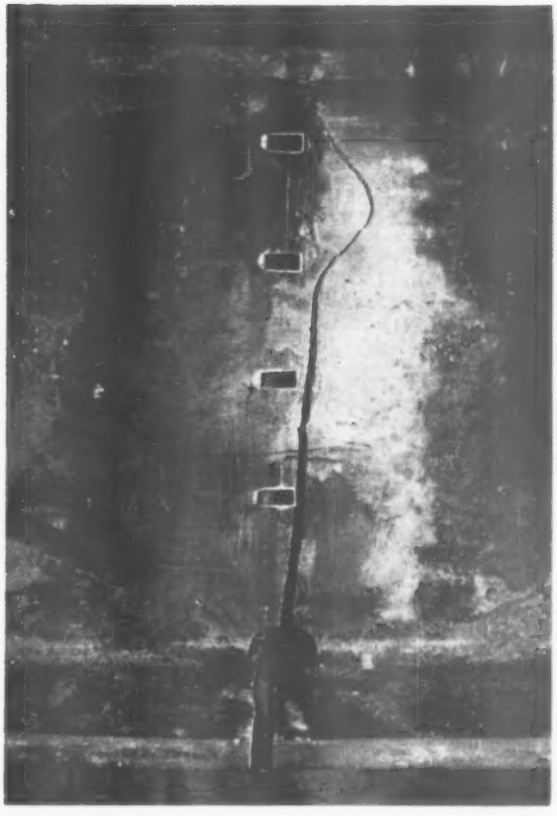


Plate 7 SK beam 3E1 stress relieved and tested at −40° F. Web and flange fractured at deflection of 0.92 in. Flange fracture about 1/4 in. outside weld

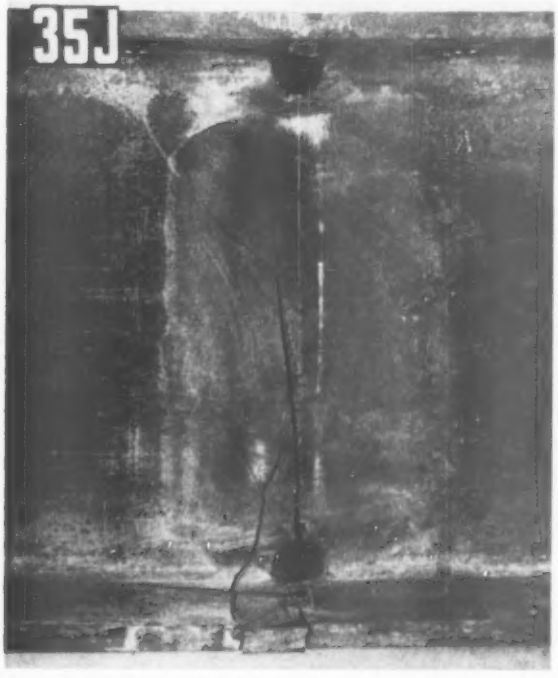


Plate 8 FK beam 6A welded with preheat, tested at −70° F. Web crack started at hole at deflection of 0.60 in. Web crack extended with multiple web and flange cracks at 1.02 in.

of the web and in a few cases started close to the lower hole.

The next grouping of fracture patterns, as the temperature decreased, included fractures in both the web and lower flange. These combined fractures occurred simultaneously or in some cases the flange break occurred at a blow subsequent to that producing the web crack. Flange cracks occurred both in the weld metal and in the base metal at 1/4 to 3/8 in. from the edge of the weld. Two presumably unfavorable conditions exist at the latter location; namely, the heat-affected zone and the higher strains directly under the edge of the hole, with the possible additional effect of gouges due to flame cutting. Fractures of this type are shown in Plates 6, 7, and 8.

At the lower temperatures, considerably below the transition range, the fractures were complete, originating and propagating through the entire web and both flanges under the same blow. Typical fractures of this type are shown in Plate 9 to 12. The brittleness at these temperatures is indicated by the irregularity of the cracks and shattering tendency. The entire fracture occurred simultaneously, with small, elastic beam deflections and strain energy absorption.

Some exceptions to the general grouping of fractures are shown in Plates 13 to 15. Nothing unusual was observed to account for the fracture in the flange under the edge of the hole without a web crack. The fracture through the lower flange remote from the welded zone and propagating in the web without regard to the splice cannot be explained except that the beam was welded with preheat extending some distance either side of the splice which may have affected the material in some indeterminate manner. This is one of those unexpected results to which every investigation is entitled.

The fracture shown in Plate 15 is of interest because of the indicated origin of the crack at a weld not associated with the splice. In testing the beams early in the program, the loading block was held in place by inverted angles having the edge of one leg welded to the top surface of the flange with fillet welds (see Plates 11 and 12). The crack appeared to originate at a small spot on one of these welds and propagated through the base metal of both flanges and web. The fracture surface was fine grained, flat without chevron markings (temp., −124° F). It is interesting to note that while the upper flange is subjected to tension during the upward vibration cycle after the blow, the magnitude of the upward deflection during this first negative cycle is approximately only one-third of the downward deflection of the first forced vibration cycle, due to the rapid damping. Therefore the fracture originated in the upper flange weld attaching the angle at nominal stresses considerably less than those to which the butt splices were subjected and must be attributed to local imperfections in these particular welds since similar attachments on other beams behaved satisfactorily. The loading block was later held in place by simple bolted connections to avoid these additional welds. The

fracture does indicate the brittleness of the material at this low temperature.

The character of the fracture surface varied depending upon whether the break was within the weld or base metal. The fracture surface in weld metal was irregular and ragged, tending to become flatter at the lower temperature. Where the break occurred adjacent to the weld in the base metal, the fracture was coarse granular and often showed a chevron pattern pointing toward the top of the lower hole in the case of the web fractures or toward the bottom of the hole in the lower flange break. These lower flange fractures usually were more irregular toward the outer edges with the fracture becoming flatter with fine grain texture directly under the hole which is in the fillet region of the beam. In the case of branching web cracks wholly in the base metal (see photographs), the fracture surface usually was comparatively flat with fine granular texture although in some cases one branch would be granular while the other was ragged, changing to fine granular near the upper flange. The fracture in the upper flange was always fine granular. The appearance of the fracture is perhaps related to the speed of propagation which is changing with the reduction of beam section. In many of the branching web fractures progressing through the base metal a fine lip was found at the edges. Occasionally these lips were present in irregular, ragged breaks but were more distinct in the fine-grained frac-

Plate 9 FK beam 6D1 tested as-welded at −35° F. Web crack started at deflection of 0.75 in., complete fracture at 1.16 in.

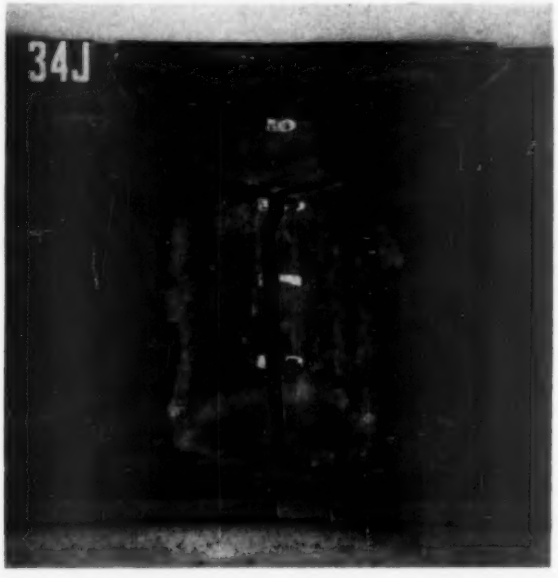


Plate 10 SK beam 5E1 stress relieved and tested at −118° F. Complete fracture at deflection of 0.60 in.

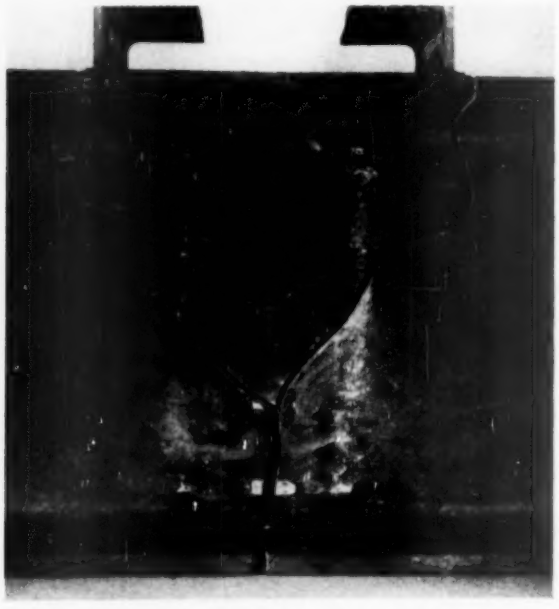


Plate 11 SK beam 3E welded with preheat, tested at −115° F. Complete fracture at deflection of 0.47 in. Early test using welded angles to hold loading block, fracture outside

Plate 12 SK beam 1E1 tested as-welded at −114° F. Complete fracture at deflection of 0.55 in. Note irregular branching cracks and fine web crack to left terminating at lower flange fillet about 20 in. from the splice

tures. No lips were found on the fractures produced at the extremely low temperature (−115° F) but were present at −60° F and higher. Additional observations concerning the formation of edge lips are given in the discussion of fractures produced when pressed notches were formed at the web hole to initiate a web crack in an unwelded beam.

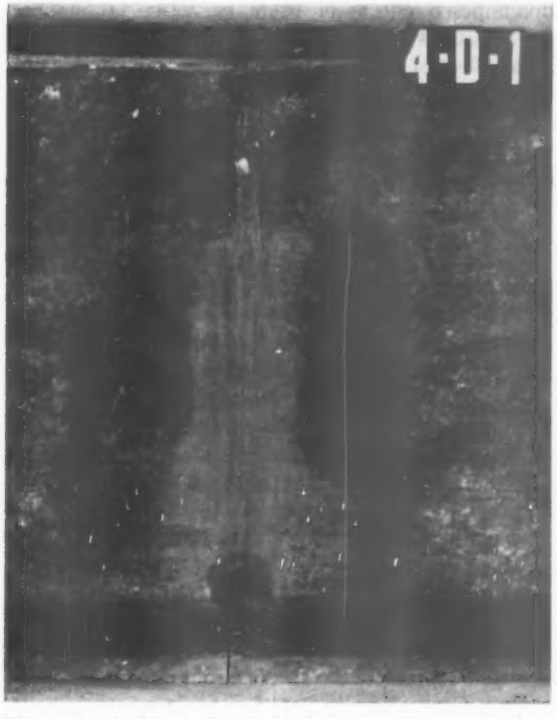


Plate 13 SK beam 4D1 welded with preheat, tested at −20° F. Fracture in flange only, outside of flange weld under edge of hole at deflection of 0.47 in.

Plate 14 SK beam 4D welded with preheat, tested at −40° F. Fracture remote from welding zone at deflection of 0.92 in.

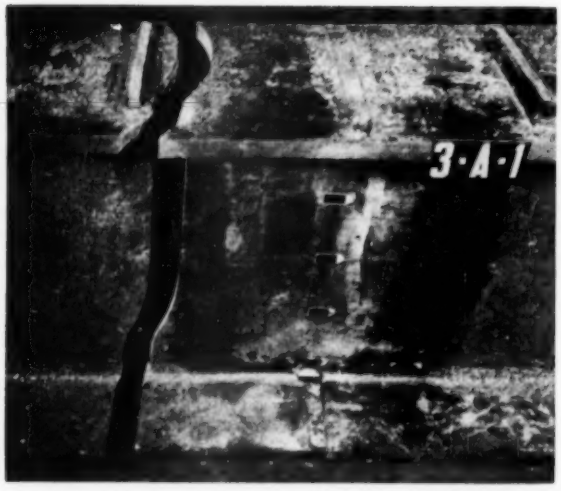


Plate 15 SK beam 3A1 stress relieved, tested at −124° F. Crack started at angle fixture weld on top flange and extended through base metal. Test discarded

STRAIN RATES

Strain rates produced by impact are not easily evaluated, particularly when flexural strains are involved, both elastic and inelastic, and local discontinuities produce indeterminate local concentrations. Even in the case of simple tension impact, the strain rate at the instant of fracture cannot be determined from the striking velocity, except that a reasonable approximation can be made when the specimen breaks without appreciable plastic deformation with a small expenditure of energy and little change of velocity of the striking mass occurs during the essentially elastic fracturing operation. Under flexural impact loading, the inertia effects, subimpact phenomena and effects of discontinuities further complicate the problem.

In the loading operation, a force (stress) is applied at a particular rate. The deformations are at first essentially elastic. When the stress reaches the yield point, the stress remains constant until considerable plastic deformation has so altered the properties as to require an increase of stress to produce further deformation. Throughout the "yield" range, the elastic strain component remains constant and only the plastic component increases. The elastic strain rate is zero in the yield range. When strain hardening develops increasing the stress required to continue deformation, the rate of increase of the elastic component is low even though the rate of plastic deformation may be high. Since it is the limiting internal force (stress) which determines fracture, the amount of plastic strain preceding fracture is important only in so far as it has altered the resistance of the material at the particular temperature. It is the rate of application of the elastic strain component which is significant in the dynamic fracturing operation, rather than the total strain rate. The magnitude of the internal stress and its rate of application, both of which are proportional to the elastic strain component, together with the properties of the material existing at the test temperature, determine the character of fracture.

A typical strain and corresponding deflection transient taken from one of the film records is shown in Fig. 19. The upper transient represents the strain on the weld at midspan of the lower flange surface. The strains in the web at the curved surface of the hole follow an almost identical pattern, differing only in magnitude. Strains at other locations, recorded simultaneously, are not shown to avoid confusion. The lower transient represents the deflection at midspan. This record was for a 30-in. drop on an SK beam tested at 0° F. No permanent sets were produced at this blow.

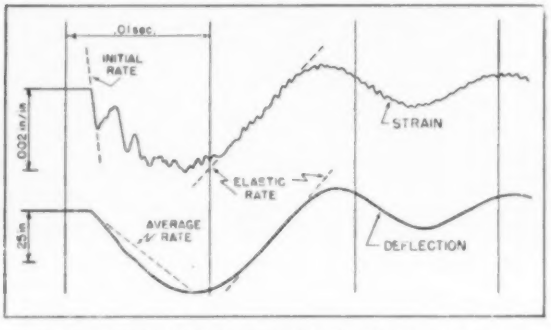


Fig. 19 Typical strain and deflection transients

The irregularities of the strain pattern reflect the superimposed effects of beam curvature, subimpact and stress waves. The deflection curve is the integrated effect of strains throughout the beam and is not sensitive to the local strain variations. The initial strain rate, immediately after contact of the tup, represented by the slope of the first serration in the strain pattern, is of the order of 4.5 in./in./sec and reaches higher values for greater heights of fall. The magnitude of the strain reached at this high rate is about 50% of the maximum strain produced by this blow. Obviously this strain is not related to the over-all beam curvature which is associated with a very small deflection in this period of time. At the instant of fracture, these vibrations would be damped by the plastic deformation accompanying the fracturing process and probably do not add appreciably to the elastic strain at fracture. Referring to the free vibration cycles after the first forced deflection of the beam, the localized strain variations indicated by the ripples superimposed on the mean curve diminish in intensity with the natural damping of the beam deflections. Similar patterns for the strains at the hole where plastic deformations are produced at an early stage are much smoother after the forced cycle.

The mean slope of the upward recovery portion of the first cycle represents the elastic strain rate due to beam action. Comparing this rate for the flange strains with that determined for the strains measured on the curved surface of the hole, it was found that the strain rate

at the hole was about two times the rate on the flange. Since the strain increments occur in the same time, this means that the magnitudes of the strains were in the same ratio. This agrees with the direct measurements made in the static tests.

Except for some of the fractures which occurred at the lowest temperatures, with relatively small heights of fall of the tup, the tension gages were broken by excessive strains before fracture and the strain rates had to be approxin ated from the available deflection record. The rate of the deflection recovery is consistent with the elastic lower flange strain rate record when converted by the elastic relation between deflection and strain. This is true also when the blow produced large permanent deflections indicating elastic action during recovery and justifying the approximation of elastic strain rates, with a concentration factor of 2 from the deflection records. In general, the fractures occurred after appreciable plastic deformation and the total measured deflections include both elastic and plastic components. However, due to the shape of the stress-strain curve for this material in which the stress remains essentially constant in the yield range, the elastic component of the deflections probably does not increase greatly beyond the value at the flexural proportional limit. Converting the elastic deflection rates to strain rates at the hole, the maximum strain rate produced in these tests is of the order of 0.90 to 1.0 in./in./sec for beams which developed structural yielding. For beams tested at −115° F which broke in the elastic range, the strain rate was about 0.80 to 0.90 in./in./sec. The time to maximum strain and deflection was 0.007 sec.

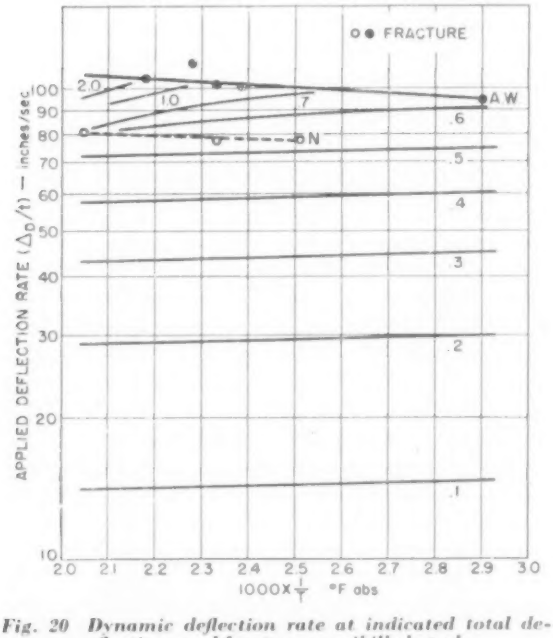


Fig. 20 Dynamic deflection rate at indicated total deflections and fracture, semikilled steel

A. W.—Fracture curve for as-welded beams. N—Fracture curve for beams with Pressed Notches. Numerical designations—Total beam deflections including sets.

As a further study of the straining rates at various temperatures the data shown in Figs. 20 and 21 were derived from the dynamic deflection records. The average deflection rate $\Delta d/t$ is taken as the slope of the line drawn from the starting point of the forced deflection cycle to the point of maximum dynamic deflections. This average slope is probably a fair approxin ation of the rate of the elastic component, particularly when large sets are produced, and $\Delta d/t$ can be taken as a measure of the strain rate. The variation with temperature of the deflection rates to produce a given deflection are shown by the lines representing increments of deflection, Δ = 0.1, 0.2, etc. The deflection rate to produce a given deflection appears to increase slightly, about 5%, with decreasing temperature over the range of these tests (−115 to +30° F). Deflections lower than about 0.50 in. represent elastic action, without permanent set. This change in deflection rate suggests some effect of temperature on the properties of the material, possibly the damping capacity.

The deflection rates, i.e., strain rates at fracture, decrease with decrease of temperature (about 12%). These average deflection rates reached maximum values of 120 to 130 ips. The fracture points for beams with pressed notches, to be discussed later, are also shown. That the deflection rates to fracture are smaller than the welded beams with holes indicates a greater concentration factor and strain at the sharp notch as compared with the web hole. The data are limited for the purposes of these comparisons because the investigation was planned primarily to determine transition temperatures and most of the tests were concentrated within this critical range, which does not provide sufficient data for a more extensive analysis of strain rates.

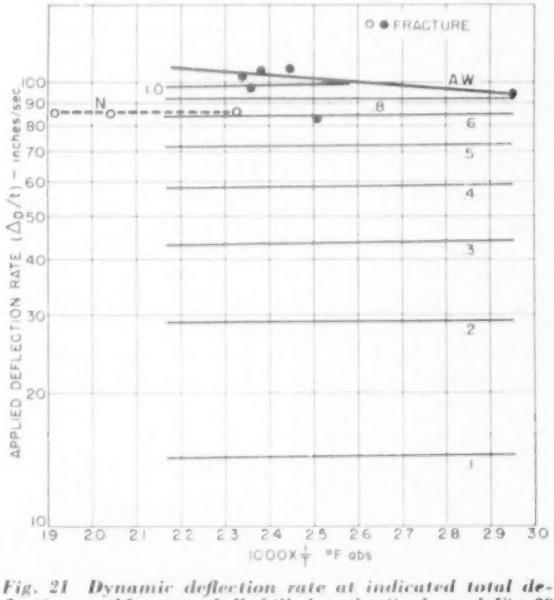


Fig. 21 Dynamic deflection rate at indicated total deflections and fracture, fully killed steel. See legend, Fig. 20

Deformations at Failure

The deflections of the beams at fracture or when the tests were discontinued due to buckling are shown in Table 5. These total deflections, including permanent sets, reflect the integrated deformation of the entire beam. Local strains were measured with electric strain gages on many of the beams, but these gages were broken in most cases long before ultimate failure of the beam. To obtain some measure of the maximum strains developed prior to failure, fine scratches were scribed on the tension flange surface and on the web directly over the hole. These scratches were made at $^1/_{10}$ in. intervals over a length of 3 in. On the flange, two longitudinal gage lines, $^1/_4$ and $2\,^5/_8$ in. from the edge were placed symmetrical with midspan, thus including the weld and extending beyond the boundaries of the web hole. The web gage line was about 0.04 in. above the edge of the hole at midlength. Distances were measured between scratches with an optical micrometer having a minimum reading of 0.0001 in.

These measurements give some indication of the amount of plastic strain developed prior to fracture. The strain associated with the fracturing process, being very local and included in a scratch interval lost by the fracture, could not be determined. Figure 22 shows typical plots of the measured strains, based on $^1/_{10}$-in. gage lengths for two beams which buckled without fracture. Beam 5E1 was FK unwelded with a drilled web hole, tested at $-112°$ F, developing a center deflection of 1.20 in. Beam 2E was SK, as-welded, tested at $+10°$ F and developed a center deflection of 2.53 in.

A summary of the data for all beams shows a marked decrease of plastic deformation preceding fracture with decrease of temperature, in both the flange and web at the hole. These variations in the flange are from about 0.2% at $-115°$ F to 8% at $+30°$ F, with a tendency for the edge strains to be smaller than those closer to the web line, particularly at the lower temperature. On the web over the hole the strains varied from 0.2% at $-115°$ F to 26% at $+30°$ F. The lower value is the limit of accuracy of the measurements. The strains on the curved edge of the hole were probably somewhat larger than those recorded above the hole.

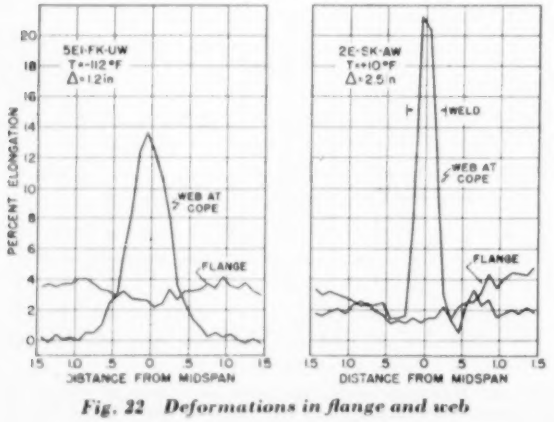


Fig. 22 Deformations in flange and web

PREPARATION OF WEB HOLES

During the progress of the impact tests, it was apparent that the presence of the web holes which, in effect, formed an internal notch, introduced a critical condition for the initiation of cracks. In addition to the geometry factor, the web weld terminated at the hole and the holes were finished to shape by flame cutting after welding, in order to remove imperfections at the "run-out." The strain concentration at the hole and the relative plastic behavior of the weld and base metals have been discussed. The possible effect of the flame-cut finishing operation on the initiation of cracks, was questioned and a few supplementary tests were made to provide some information.

Four unwelded beams, as-rolled, were provided with web holes, simulating those used in the welded beams, by piercing the web and shaping to size by oxy-acetylene flame. This piercing operation introduced more local heat than the finishing after welding with the likelihood of inducing greater residual stresses and the edges were somewhat more irregular due to the difficulty of using the torch so close to the flange surface. The residual stresses induced are not likely to have contributed to the failures since plastic deformation preceded the fractures. Four beams of each of the two materials were tested under impact at various temperatures. The results are recorded in Table 5. The FK beams fractured with cracks starting at the holes, at or below $-40°$ F and buckled at $-20°$ F, suggesting a transition temperature of about $-30°$ F which is the same as the as-welded beams. The SK beams fractured at $-115°$ F but buckled at $-60°$ F and higher. The temperature of transition from buckle to fracture is not closely defined because of the limited number of specimens but it is apparent that the preparation of the holes had an unfavorable effect since beams tested with drilled holes buckled without fracture at $-115°$ F. A comparison of the behavior of the two steels is not justified by these few tests. The effects of manual flame cutting are likely to be variable, particularly with respect to roughness of the finished edge and accidental gouging. Two of the FK beams cracked in the flange only, at the edge of the hole, which was unusual.

It was not the purpose of these tests to investigate the physical and metallurgical aspects of flame cutting, but in view of the above results, an examination was made of flame-cut edges at the hole. Samples were taken from four previously tested welded beams, which had cracked at the hole adjacent to the tension flange but finally buckled. The samples were taken at the upper hole where the material had been subjected to stress but was uncracked. Plate 16A shows micrographs, originally taken at 500 ×, of the edge of the base metal, outside the welding heat-affected zone, from a FK beam. Both steels showed similar conditions. Plate 16*B* is a micrograph, from the same specimen, originally taken at 1000 ×.

Plate 16*A* shows multiple surface cracks existing in a hard transformation layer (white) having an average

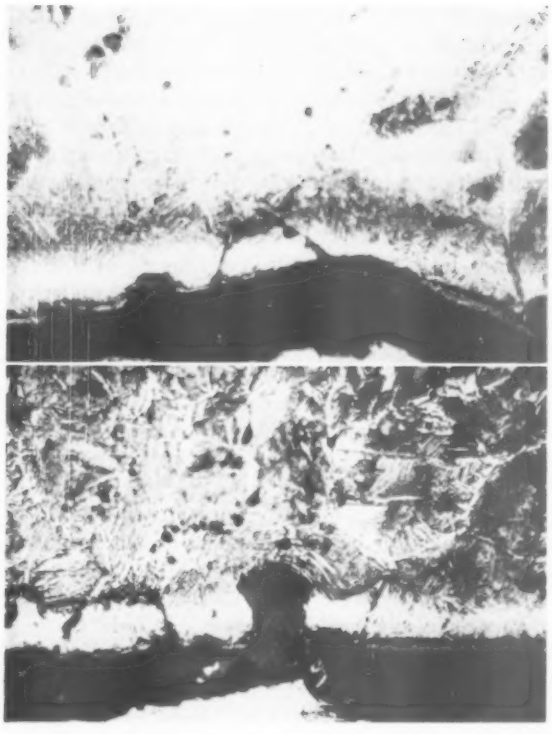


Plate 16A Micrographs of flame-cut edge of base metal at hole

depth of about 0.0015 in. and an average hardness of Converted Rockwell "C" 32. It will be noted that oxide has penetrated into the cracks. The structure back of the hard transformed layer consists of a typical overheated steel structure to a depth of perhaps $^1/_{16}$ in. Plate 16*B* is a further illustration of multiple cracks in a hard transformation layer at the flame-cut edge. The cracks are included in a hard and more brittle material tending to terminate upon entering into the relatively softer overheated zone having an average hardness of Converted Rockwell "B" 95. These hardness measurements were made on a Bergsman Micro Hardness Tester employing a 5-g load. The presence of oxide in these

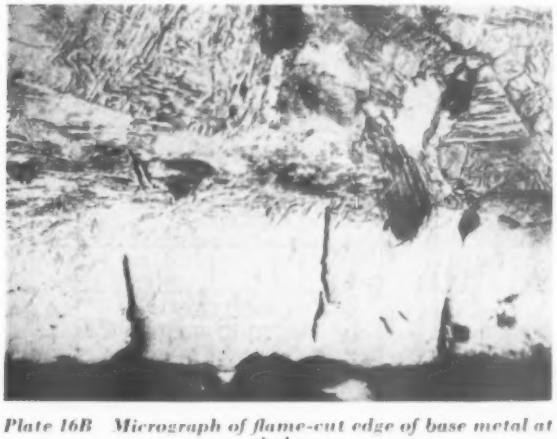


Plate 16B Micrograph of flame-cut edge of base metal at hole

cracks indicate that the formation was associated with the heating cycle, not due to subsequent load stresses and represent thermal cracks probably developed during the rapid cooling.

The shallow cracks at the flame-cut edges are stress raisers, which should invite the initiation of major cracks whose propagation depends on the properties of the base metal at the test temperature. It should be noted that in preparing the beams for impact test, the upper edge of the lower hole, including the weld and adjacent base metal, was smoothed with a file for gage placement. This filing probably removed the shallow cracks present at the critical region but may not have removed the heat-affected zone. The corners of the hole adjacent to the flange surface were not filed. In some cases a shallow gouged groove was produced by the cutting at the corners, which together with the edge condition and the discontinuity produced by the hole produced a critical region in the flange, where some of the fractures did occur.

While the edge effects of flame cutting are unfavorable at a stressed edge subjected to high strain rate at low temperatures, the tendency to crack propagation is reduced at higher temperatures by the higher energy absorbing capacity of the base metal. Other reported tests indicate the visible start of cracks at flame-cut edges to be independent of the temperatures used or type of subsequent fracture, and higher fracture stress for smoothly ground surfaces[3] under static loads. Also the effects of flame cutting in preparing the edges for welding are unimportant because of the subsequent fusion by the welding operation.

In the type of splice tested, the web holes were considered necessary to facilitate the flange welding. What would have been the result if no holes were present, assuming that a sound weld could be made, or could the shape and formation of the hole be modified to improve the beam behavior at low temperature? To answer the first question, a few additional tests were made in which the splice was made by continuous welding of the web and flanges without web holes. A trial weldment indicated apparently satisfactory weld soundness and the practicality of the procedure.

Assuming that the holes are necessary for fabrication, why not fill the holes with weld metal after completion of the splice, thereby removing the stress concentration and edge effects of flame cutting? This procedure was considered but rejected because of the danger of imperfections which might be more unfavorable than a well-formed hole. The remaining alternative then concerns the best method of forming the hole. The suggestion has been made to elongate the hole in the direction of the flange, thus reducing the curvature of the edge facing the web and moving the flange strain concentration further from the weld zone. The hole might also be enlarged without seriously reducing the web strength.

Practical considerations require that web holes, whatever their shape and size, be formed by flame cutting. Although the presence of microcracks at the cut edges would not be eliminated by postheating, such

treatment would tend to improve the properties of the heat-affected edge zone with less likelihood of propagation of these shallow cracks at temperatures above the critical propagation temperature of the material. It will be noted that beams of both steels were tested after stress-relief treatment following fabrication. This postheating of the entire beam to 1150° F produced no change in the transition temperature of the SK welded beams as compared with the as-welded conditions. Some improvement was produced in the FK beams. It is not clear, however, that the relative behavior due to this post heat can be attributed to any influence of flame-cut edges of the welded beams.

The alternate fabrication procedures which have been discussed in view of the findings of these tests are of interest to the designer and warrant further trial and study. Some tests were made on beams spliced with a continuous weld without web holes for comparison with the beams of the original program. The results are recorded later in this report.

CRACK PROPAGATION

In the previous discussion of the fractures produced, it was indicated that the extent of the cracks varied with the test temperature. At temperatures well below the transition to buckling, shattering failures resulted with branching cracks extending laterally in the web and through the upper flange. At somewhat higher temperatures the cracks did not include the upper flange, diminishing until only part of the web cracked. At the threshold of transition, cracks at the web hole reduced to short tears which did not propagate even when impact loads were increased to ultimately produce buckling.

The extent of the cracked section, expressed as a percentage of the depth of the section and plotted as a function of the test temperature, is shown in Fig. 23. No attempt is made to include the exact length of branching cracks, which sometimes extend a considerable distance in the web, or to assign relative weights to web and flange cracks. The curves should terminate at temperatures producing no crack, corresponding to the transition temperature according to the crack-buckle criterion. Beam specimens are not suitable for a study of crack propagation. It will be noted that partial fractures tend to terminate at midheight or slightly above, where the flexural stresses are low or the crack enters a compression field. The results do, however, suggest a relation between the extent of crack propagation and temperature. The web cracks in the welded beams were usually in or adjacent to the weld metal. To further investigate the effect of temperature on the extent of free-running cracks without welding complications, a few unwelded beams were prepared with sharp pressed notches at the top of a drilled web hole. Retaining the geometry of the welded beams the notch served as a crack starter in the web. The tool used to form the notch was ground to a sharp edge which was slightly rounded on polishing paper. In order to minimize upsetting of the material by pressing the notch, a notch slightly undersize was first formed with a file. Pressure on the inserted tool then shaped the notch to its final contour. Plate 17 is a micrograph of a section taken through the notch at about mid-thickness of the $^1/_2$-in. web material. Only a slight distortion of the structure is noticeable at the root of the notch. From measurements of this micrograph, the radius of the root was 0.00075 in. The notches were 0.1 in. deep (above hole edge) with 45-deg included angle.

Three unwelded beams of both as-rolled materials with pressed notches were tested under impact at different temperatures. Due to the limited number of tests with only the welded beam results to predict per-

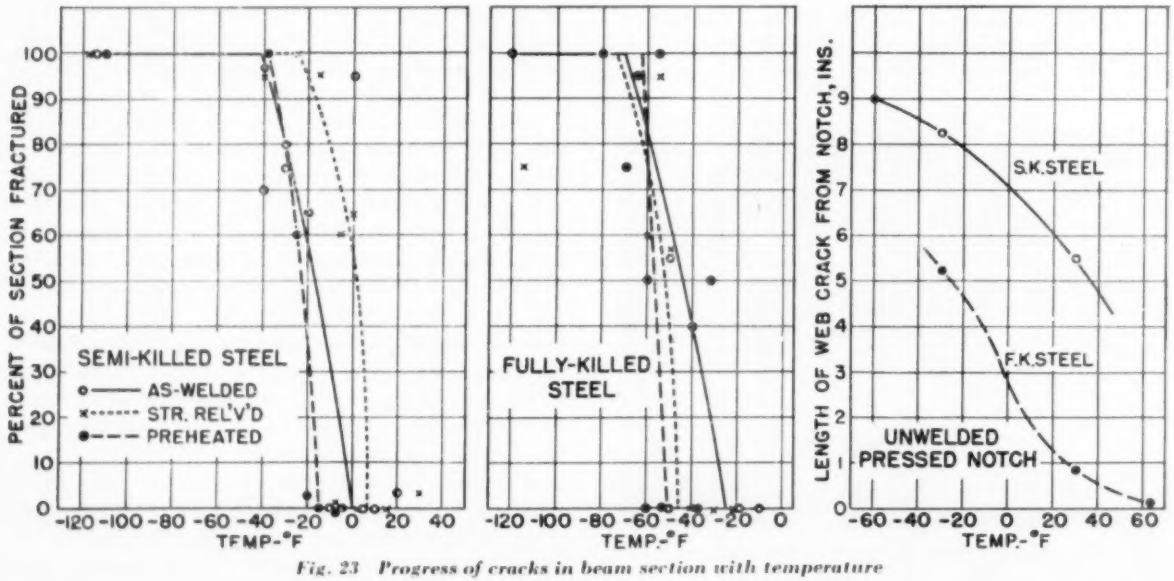


Fig. 23 Progress of cracks in beam section with temperature

Plate 17 Micrograph at root of pressed notch used as crack starter

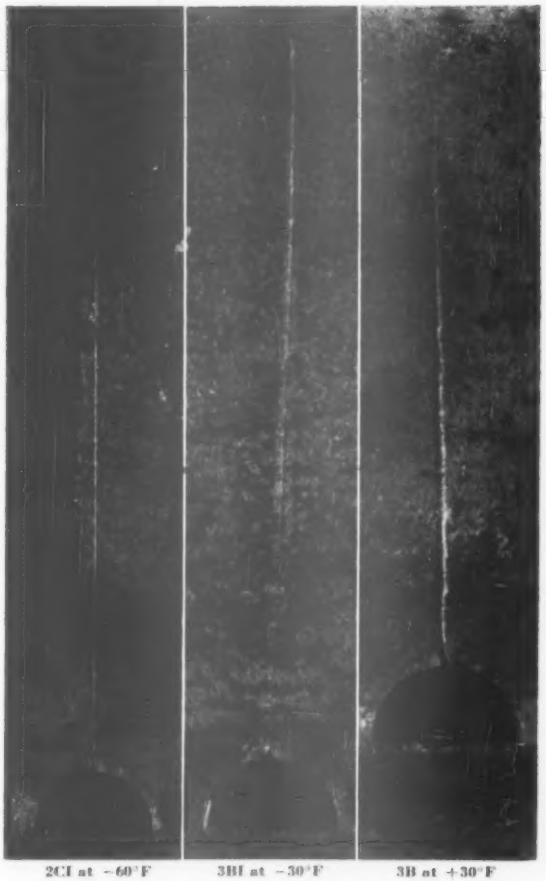


Plate 18 Fractures in unwelded and notched beams, SK series

formance, the temperature range of the tests is rather wide, but the results indicate a decided difference in the tendency to permit free-running cracks in the two materials. The test data are shown in Table 7. The lengths of the initial cracks originating at the notch

Table 7—Propagation of Cracks Induced by a Pressed Notch

Mark	*Steel*	*Test temp., ° F*	*Drop at crack, in.*	*Beam deflection, in.*	*Strain energy to crack, ft-kips*	*Length of crack, in.*	*Fracture*
2C1	SK	−60	48	0.60	6.4	9	Granular
3B1	SK	−30	42	0.53	5.3	8 1/4	Granular
3B	SK	+30	42	0.60	6.2	5 1/2	Granular
2D1	FK	−30	54	0.69	7.5	5 1/4	Granular
3D	FK	+30	48	0.66	7.6	1/2, 7/8	Granular (see note below)
2D	FK	+63	54	0.91	11.7	1/16	Fibrous

Specimen 2C1, SK at −60° F. The fracture is rough granular with chevron pattern pointing toward the notch over the lower 7 in. changing to fine granular near the end of the crack. No measurable change of thickness occurred over the entire length. Sectioning of web, at the end of the crack, showed that the crack advanced further in the center of the web thickness (0.485 in. thick) by 0.10 in. as compared with end of crack at the web surfaces.

Specimen 3B1, SK at −30° F. The entire fracture is fine granular with chevron pattern on the lower 5 in. Thin silky lips at the edges in the upper 2 in. extend about 0.02 in. deep. No lips were near the notch. No measurable change in thickness occurred near the notch but the thickness decreased by 0.003 to 0.010 in. adjacent to the crack over its upper length. The granular fracture was finer at the terminating end.

Specimen 3B, SK at +30° F. The web surfaces along the line of the entire crack were necked on both sides to reduce the thickness by 0.025 in. with a small dimple at the terminating end. Within this necked zone, the fracture extended to the surfaces as a fine crack for about 4 in. from the notch. This fracture was rough granular with a chevron pattern pointing to the notch, except for surface lips extending along the entire length (including part near notch) which were about 0.02 in. deep. These lips were smooth shear fractures. The fracture in the upper 1 1/2 in. of its length was *internal;* not extending through the neck at the surfaces. (See Plate 18) The section remaining intact was about 0.02 in. deep.

Specimen 2D1, FK at −30° F. The fracture was rough granular with chevron pattern. Edge lips are 0.01 deep near notch and 0.02 in. deep further away. No appreciable change of thickness occurred near the notch, but the thickness decreased 0.01–0.02 in. in a narrow necked zone. The crack terminated in a dimple.

Specimen 3D, FK at +30° F. The first crack was 1/2 in. long at the 48-in. blow. The crack extended to 7/8 in. long at the 84-in. blow when the beam ultimately buckled. The fracture is granular with smooth edge lips. The crack terminated in a dimple or depression where the thickness decreased 0.065 in. (original 0.485 in.).

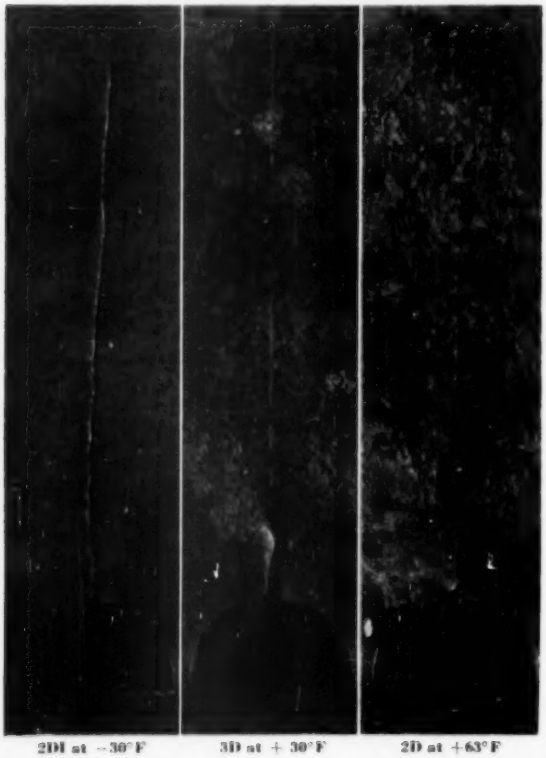
Specimen 2D, FK at +63° F. A small tear, 1/32 in. long, formed at the notch at the 54-in. blow. This crack became 1/16 in. long at the 84-in. blow when the beam buckled. The crack formed at 45 deg to the vertical through the notch. The fracture appears rough fibrous. A dimpled zone at termination had a thickness reduction of 0.045 in.

and extending vertically into the web are shown graphically at the test temperatures in Fig. 23.

The cracks developed in the SK series are shown in Plate 18. In the view of Specimen 2-C-1 (−60° F), the surface adjacent to the crack is not visibly changed. (The scribe line, vertically from the notch, is not to be confused with the cracks in these views.) The surfaces adjacent to the fine crack in Specimen 3B1 (−30° F) is slightly depressed. Plastic deformation is suggested by the lighter zone along the crack. The view of Specimen 3B (+30° F) shows a pronounced local decrease in thickness along a vertical line starting at the notch. Close examination showed a very fine crack in the root of this necked-down zone over the lower 4 in. of its length. The internal condition is discussed more fully below.

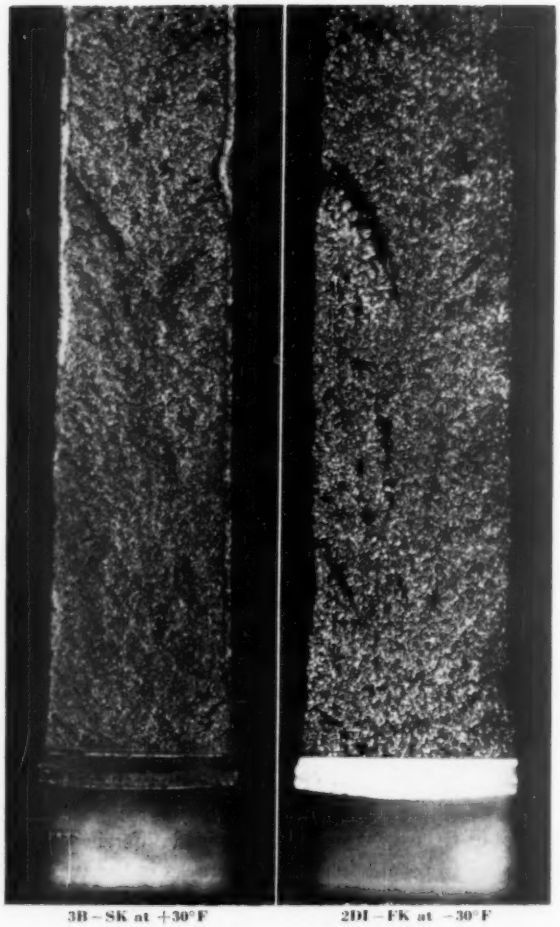
Plate 19 shows the crack formations in the FK series. Specimen 2D1 (−30° F) showed a small and narrow localized necking at the crack. The short crack above the notch in Specimen 3D (+30° F) did not propagate beyond its initial formation with subsequent blows which produced considerable deformation of the web as evidenced by scaling and strain lines. The crack terminates in a pronounced depression or dimple. The smaller crack in Specimen 2D (+63° F) is more properly described as a tear which did not propagate with subsequent blows.

The fractures near the notch of Specimen 3B, SK tested at +30° F and specimen 2D1, FK at −30° F are shown in Plate 20. The smooth lips at the edges



2D1 at −30° F 3D at + 30° F 2D at +63° F

Plate 19 Fractures in unwelded and notched Beams, FK series



3B − SK at +30° F 2D1 − FK at −30° F

Plate 20 Fractures near notch of specimen, edges show smooth lips

are visible as lighter zones. The fracture of Specimen 3B provided interesting information concerning the formation of these cracks. The web surfaces along the line of the entire crack were necked in on both sides. Only after close examination was a very fine crack found at the bottom of the narrow necked zone over the lower 4 in. of its length. The appearance of this fracture is shown in Plate 20. A section cut through the thickness of the web where the surface was necked without visible surface crack, showed that the crack was *internal* leaving the material intact near the original surfaces for a depth of about 0.02 in. This internal fracture is shown in Plate 21*A*. It is this thin surface zone which when ruptured shows a smooth shear fracture and forms, what has been called a "shear lip," at the edges. In Plate 21*A*, the view includes the full thickness of the web across which the crack extends. One edge clearly shows the necked-down surface. The opposite surface had been ground and polished for etching, which removed the depression similar to that on the other surface. This polished surface, after Fry etching, is shown in Plate 21*B*. The etching (dark zones) shows plastic deformation in a zone about $\frac{1}{8}$ in. wide along the entire crack starting at the notch. At

the termination of the crack the branching strain lines indicate dissipation of energy by shear deformation ahead of the crack.

Similar etching of Specimen 3B1, SK at −30° F showed no appreciable plastic zone adjacent to the crack but a few branching strain lines spread out beyond the end of the crack. The etching process was not effective on any of the FK specimens.

The condition near the terminating end of the crack formed on Specimen 2C1 SK at −60° F, was examined by sectioning the web. At the center of the web thickness (0.485 in.), the end of the crack had advanced 0.10 in. ahead of the end of the crack at the surfaces. This suggests faster-propagation at the center, with a lag and modified fracturing force on the section nearer the edges which may be associated with the irregularities of the fracture surface referred to as chevron or herringbone. The presence of a lip at the edges of a fracture can also be interpreted to indicate prior internal fracture. Whether this condition is due to the propagation phenomena or different properties of the material, such as a skin effect, at the surfaces of the rolled plate is not indicated by these data.

Referring to the data in Table 7 and Fig. 23, both series of tests indicate that the tendency for crack propagation decreases as the temperature is raised. In each series composed of three tests, the striking velocity, beam deflection and hence the strain rate at the root of the notch, when fracture occurred, was nearly the same. The initiation of cracks at the notch appears therefore to be independent of temperature over the range of 90° F in these tests. The length of crack, however, is primarily related to the temperature. Comparing the two series, the cracks were initiated at lower strain rates on the SK steel. The tendency to propagate the crack, as measured by its length, at the same temperature is also more pronounced in the SK steel. The crack length in the SK steel at +30° F (with lower initial strain rate) was about the same as that in the FK steel at −30° F, a difference of 60° F. In the FK series, the cracks initiated at the higher temperatures did not propagate with subsequently increased impact loads and the ability of the beams to resist loads up to normal buckling refusal was not impaired.

It appears that above some critical temperature, cracks do not propagate. The lowest temperature inhibiting free running cracks is a more rational and useful definition of transition temperature than one based on factors related to the initiation of a crack or physical characteristics of the fracture itself, which,

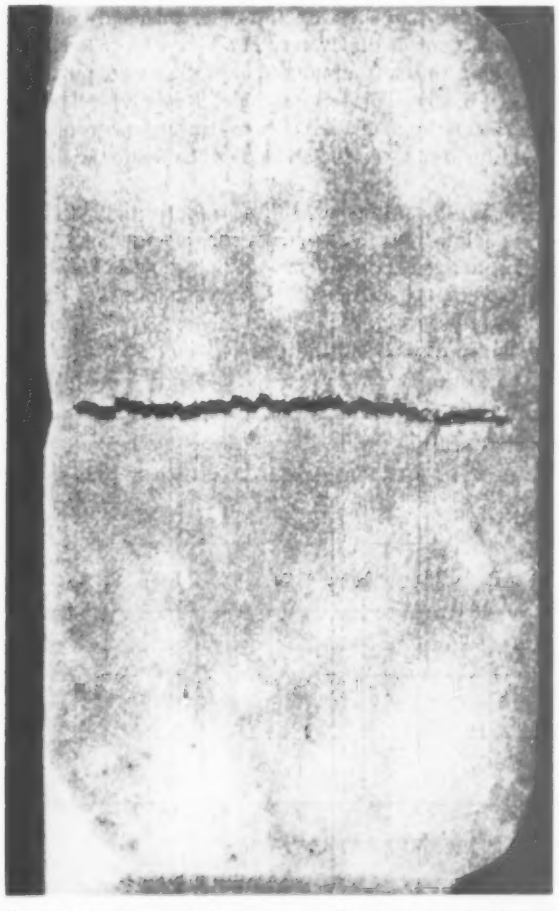


Plate 21A Internal fracture of web of Specimen 3B, SK +30° F. Note necked-down and intact surface

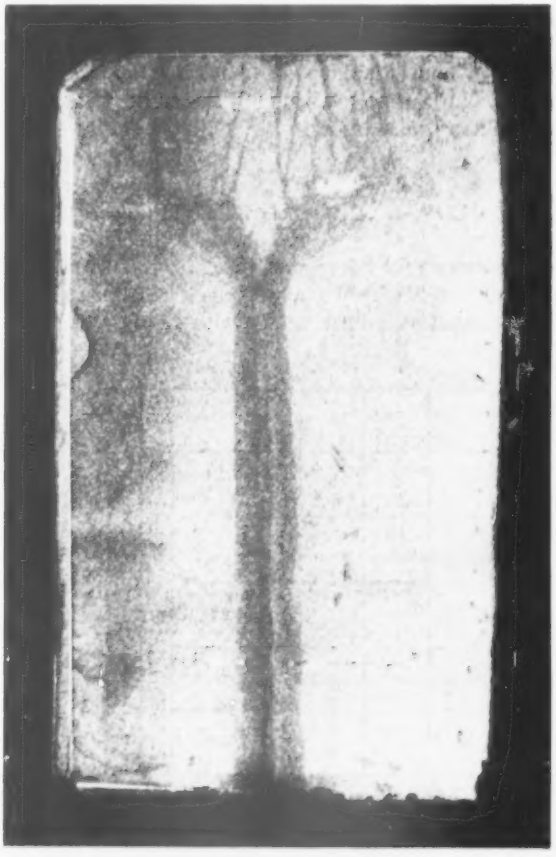


Plate 21B Surface of web at termination of crack which does not extend through necked surface. Dark zones produced by Fry etch indicate plastic deformation

however, may reflect the propagation phenomena. From a practical viewpoint, the presence of small cracks, produced by fabrication or erection processes, which are known to exist, do not in themselves impair the serviceability of the structure. They do, however, serve as a starting point for extension when the stress intensity and the properties of the material at the operating temperature are critical. It may be argued that without crack initiation, the question of crack propagation is irrelevant and suitability for service is thus determined by the temperature at which cracks start thereby introducing a propagation potential. However, it affords little assurance to delay cracking by a few degrees if the crack, once started, leads to complete destruction of the member, as would be the case in a material lacking propagation toughness.

While the data are too limited to warrant quantitative comparisons, the curves in Fig. 23 are displaced by about 60° F. Assuming that the curves are nearly parallel when the SK curve is continued to a nonpropagating temperature, it could be concluded that the SK steel reached a critical temperature favorable to propagation about 60° F higher than the FK steel and that these temperatures are of the order of +25° F and +85° F, respectively. Recognizing that liberties are being taken with limited data, these temperatures and their difference are in fair agreement with the transition temperatures derived from the V-notch Charpy tests at the 50 ft-lb level, for the specimens taken near the fillet and adjacent web (see Table 3, locations 3 and 5) but bear no relation with the results of the keyhole-notch Charpy tests.

LOW-HYDROGEN ELECTRODES

The beams for the major series of tests in which as-welded, stress-relieving and preheat treatments were investigated were butt welded with E6011 and E6020 electrodes. The results showed lower transition temperatures for the FK steel. It was of interest to determine whether the use of low-hydrogen electrodes would improve the performance of both steels, whether these electrodes with SK steel would accomplish the same result as using FK steel as originally welded and whether the advantages of preheat might be duplicated.

One series of tests was made on beam specimens butt welded with E6016 electrodes (without moisture control) including both steels, as-welded and with preheat. No change of welding procedure was made other than to use d-c reverse polarity current instead of the 25-cycle a-c current used in the original series. The electrodes were used out of fresh boxes as received from the storage room. It is important to note that no attempt was made to control the moisture content of these electrodes.

To determine the influence of moisture control a second series of tests was made on specimens welded with E6016 electrodes conforming to Grade MIL-E-180-16, Federal Specification MIL-E-986A. These electrodes were packaged in 40-lb hermetically sealed containers, which were opened as required and the electrodes were placed in an electric oven at a temperature of 250° F. Electrodes remained in the oven overnight before being used the following day. All welding was done on the day shift. The welder took only enough electrodes out of the oven to last him 4 hr or less. At the end of the shift all unused electrodes were put back into the oven. This series included only FK steel beams (SK beams unavailable). Four specimens were provided for test as-welded and also when welded with preheat.

The results of tests on specimens fabricated with both types of low-hydrogen electrodes are recorded in Table 8 giving derived data similar to that shown in Table 5 for the original test series. The results are also shown graphically in Fig. 24.

The transition temperatures derived from these tests,

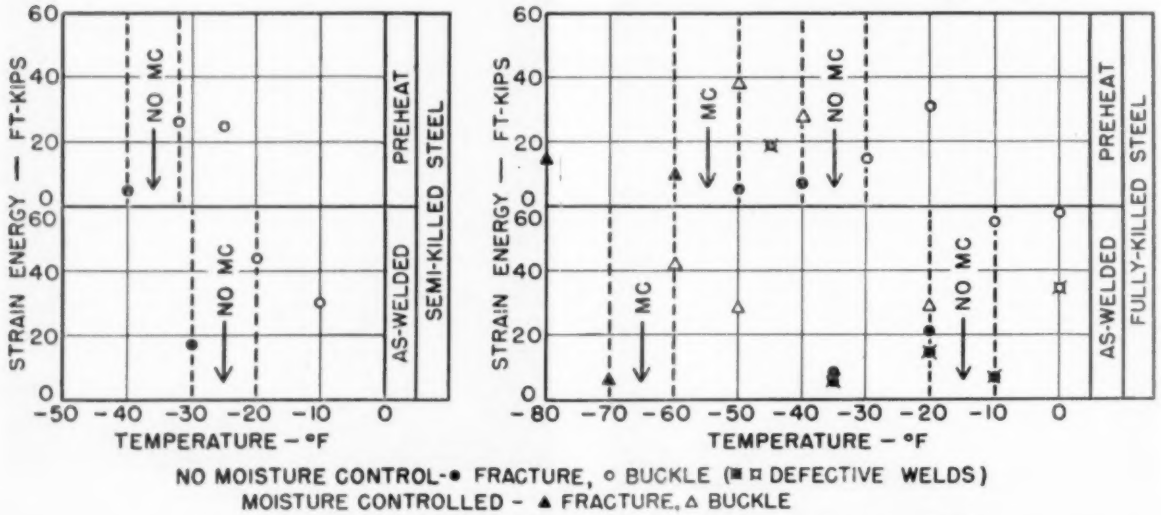


Fig. 24 Fracture-buckle transition temperatures, low-hydrogen electrodes, E6016

Table 8—Results of Impact Tests on Butt-Welded Beams, Low-Hydrogen Electrodes

Mark	Temperature, ° F	Height of fall, in.*	Energy, ft-kips,†	Deflection, in.††	Equivalent loads, kips PL	Equivalent loads, kips YP§	Ratio, YP/PL	Stresses, ksi PL	Stresses, ksi YP	Failure
Semikilled steel, as-welded, E6016 electrodes (no moisture control)										
5C1	−30	78–108	17.0–51.2	1.10–2.75	210	260.5	1.24	65.2	80.9	Web fracture to complete
1B1	−20	108	44.3	2.43	190	250	1.315	59.0	77.6	Buckled
1B	−10	96	29.8	1.55	190	241	1.27	59.0	74.8	Buckled
						Avg	1.275			
Semikilled steel, preheat and welded, E6016 electrodes (no moisture control)										
3C	−40	42	4.7	0.50	...	...	...	...	...	Web and lower flange fracture
4E1	−32	96	26.5	1.59	218	255	1.17	67.8	79.2	Buckled
5B	−25	90	25.2	1.58	199	243	1.22	61.8	75.5	Buckled
						Avg	1.20			
Fully killed steel, as-welded, E6016 electrodes (no moisture control)										
5C	−35	42–72	5.6–16.7	0.55–1.10	202	244	1.21	62.7	75.8	Web and lower flange fracture**
7C	−20	66–96	15.2–36.1	1.05–2.22	185	238	1.29	57.5	73.9	Web fracture to buckle**
5C1	−10	48–70	7.2–25.3	0.64–1.61	182	230	1.26	56.5	71.5	Web and lower flange fracture**
4C1	0	96	33.8	2.12	167	228.5	1.37	51.9	71.0	Buckle**
2A	−35	48–60	6.8–11.6	0.61–0.72	232.5	...	...	72.3	...	Web to lower flange fracture
1A	−20	78	20.8	1.12	205.5	245	1.19	63.8	76.1	Web and lower flange fracture
4A	−10	110	54.6	2.98	178	234	1.32	55.3	72.6	Buckled
3A	0	110	57.9	3.19	167.8	224	1.33	52.1	69.5	Buckled
						Avg	1.28			
Fully killed steel, preheat and welded, E6016 electrodes (no moisture control)										
3A1	−45	78	18.8	1.10	200	258	1.29	62.1	80.1	Web and lower flange fracture**
2E	−50	42	5.4	0.50	195	...	...	60.5	...	Bottom flange only (hole edge)
2A1	−40	48	6.8	0.61	217	...	...	67.4	...	Bottom flange only (hole edge)
4A1	−30	78	14.4	0.99	187	238	1.27	58.0	74.0	Buckled
1A1	−20	96	30.9	1.87	185	236.5	1.28	57.5	73.5	Buckled
						Avg	1.28			
Fully killed steel, as-welded, E6016 electrodes (with moisture control)										
2E1	−70	54	6.0	0.57	230	...	...	71.5	...	Complete fracture (small defect)
10B	−60	108	41.9	2.23	217	265.5	1.22	67.4	82.5	Buckled
10D	−50	96	28.6	1.66	210	253.0	1.21	65.2	78.6	Buckled
6E	−20	96	29.0	1.80	180	228.5	1.27	55.9	71.0	Buckled
						Avg	1.23			
Fully killed steel, preheat and welded, E6016 electrodes (with moisture control)										
10B1	−80	78	14.4	0.94	220	284	1.29	68.3	88.2	Complete fracture 6 in. from splice
6E1	−60	60	9.2	0.73	211	...	...	65.5	...	Web and lower flange
3D1	−50	102	38.0	2.15	208	256	1.23	64.6	79.5	Buckled
10D1	−40	96	28.3	1.72	190	250	1.32	59.0	77.6	Buckled
						Avg	1.28			

NOTE: See Table 5 for footnotes* to §.
** Defective welds and gouged copes

based on the fracture vs. buckle criterion, for the three types of electrodes used, are summarized in Table 9.

Referring to the series using E6016 electrodes without moisture control, the transition temperature was lowered for the semikilled steel by 20° F in both the as-welded and preheat conditions. Also the use of these 6016 electrodes on the SK steel in the as-welded condition produced about the same favorable effect as welding with preheat when using E6020 electrodes.

However, the use of these E6016 electrodes was unfavorable when applied to the FK steel in both the as-welded and preheat conditions. The critical temperature was raised by 15 and 20° F. When this unexpected reversal of effects on the FK steel beams was becoming evident during the progress of the tests, it was suspected that weld imperfections were responsible. Inspection of fractures disclosed some lack of fusion in the roots of the web welds. So as not to invalidate the conclusions concerning the electrode variable, additional beam specimens were fabricated and untested beams from the same lot were cut and rewelded with the two

Table 9—Effect of Type of Electrode on Transition Temperatures, ° F, of Butt-Welded Structural Beams

Type of steel, weld treatment	Type of electrodes: E6011, E6020	E6016, No moisture control	E6016, Moisture controlled
Semikilled			
As-welded	−5	−25	...
Preheat	−17	−36	...
Fully killed			
As-welded	−29	−15	−65
Preheat	−57	−35	−55

halves reversed so as not to introduce possible effects of previous welding. The results of tests on these replacements confirmed the previous tests, thus strengthening the conclusion that the transition temperature was raised and eliminating the suspected influence of weld imperfections. The lack of complete fusion, not found in previous weldments indicates that attention must be given to proper welding manipulation when using this type of electrode.

It has been recorded that the E6016 electrodes used for these weldments were taken from normal shop storage without special moisture control. An assumed unfavorable influence of moisture does not explain the inconsistency in behavior of the two grades of steel except that the electrodes may have contained different amounts of moisture, and the electrodes used for the SK series may have been more recently processed. The exactly opposite effect on the two steels might indicate that the combined properties of the steel and electrodes enter into the over-all behavior of the weldment in some indeterminate manner and a generalization as to the effectiveness of low-hydrogen electrodes, without moisture control, is not warranted.

A comparison of weldments with E6016 electrodes with moisture control is limited to FK steel since the supply of SK steel at this stage of the investigation was exhausted. These electrodes reduced the transition temperature in the as-welded condition by 35° F as compared with the same condition using E6020 electrodes. These electrodes apparently corrected the unfavorable influences of the E6016 electrodes originally used, presumably due to moisture control. However, when welded with preheat, the moisture controlled electrodes gave no improvement over the E6020 electrodes although they did avoid an increase of the critical temperature shown by the electrodes without moisture control. The results indicate that weldments with the moisture controlled electrodes as-welded are comparable to those fabricated with E6020 electrodes with preheat for the fully killed steel. In fact, these electrodes gave the most favorable results of any of the electrodes used.

These comparisons, based on the test results, give some information on the relative effects of the use of low-hydrogen electrodes with and without preheat. It is contended that hydrogen in combination with straining produces microcracks, which form during the rapid cooling after welding. Investigations with small specimens have shown that either preheat or the use of low-hydrogen electrodes will avoid microcracks, presumably because they both reduce the hydrogen content. Confirming this conclusion, these tests show that the critical temperature for beams welded with E6020 electrodes and preheat was about the same as for those welded with moisture-controlled E6016 electrodes as-welded, on the same steel (FK). If reduction of hydrogen content is the desired objective and both preheat and low-hydrogen electrodes are equally effective, then it would appear that, in welding the steel used in this investigation, no appreciable additive advantage would be obtained by preheating when using low-hydrogen electrodes. The limited number of tests on FK beams welded with moisture-controlled E6016 electrodes shows no improvement by using preheat over the as-welded condition.

The results of tests with electrodes having no moisture control (Fig. 24) appear contradictory to the above interpretation. However, these electrodes were not actually "low hydrogen" and the improvement shown by preheat treatment is consistent with the above reasoning. The effect on the critical temperature in either case, compared with the E6020 electrodes was not the same in the two steels. It should be noted that beam specimens were fabricated in lots at different times with the liklihood of different moisture contents in these uncontrolled electrodes. The moisture content was not determined because the effects were not suspected at the time of fabrication. While the preheat temperature was reasonably controlled for all specimens, the possible effect of varying this temperature under these conditions is not known. The interrelated variables of steel properties, electrodes, moisture control and preheat temperatures require further study.

WEB SPLICES WITH CONTINUOUS WELDS

A series of impact tests were made on six FK steel beams, butt welded without web holes. The electrodes used in these weldments were E6011 and E6020 to afford comparison with the extensive tests made on beams with web holes, in the as-welded condition.

The shop welding procedure for these splices is indicated in Fig. 25 and is briefly described as follows:

Preparation. The ends of the flanges and web of each beam were beveled by machine flame cutting to form a 60-deg, single-V welding joint with $^1/_{16}$-in. land and $^1/_8$-in. root opening. The flanges were beveled from the inside. At the corners where the web intersects the flanges, hand cutting was employed to insure a minimum opening of $^1/_8$ in. (see Fig. 25). Beams were assembled and tack welded on the outside of the flanges. The beam was positioned for each weld to permit welding in the flat position, except for short end returns as noted.

Web and Flange Welding. The sequence of the welding operation was as follows:

1. The web weld was completed first—refer to Fig. 25. The web root weld, W1 was made with $^5/_{32}$-in. E6011 electrodes. Subsequent web weld passes, W2, W3, etc., were deposited using $^3/_{16}$ in. E6020 electrodes to fill the groove. Each pass was started in the corner at the fillet and then run across the length of the web, ending in the opposite corner slightly above the fillet. The direction of welding was alternated first from left to right, then from right to left. The short end returns into the flange groove were made with the web passes while the web was in the horizontal position.

2. After the web bevel was completely welded, the

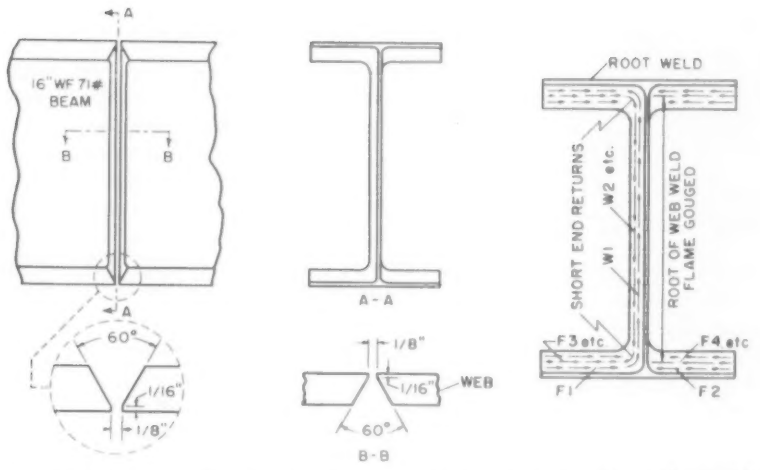


Fig. 25 Edge preparation and sequence of welding for butt welds without web holes

beam was turned over and the root was flame gouged to sound metal.

3. Two passes were deposited on the root side of the web weld using $^3/_{16}$-in. E6020 electrodes to complete the web weld.

4. Root passes, F1 and F2, on opposite sides of the web, in the first flange welded were made with $^5/_{32}$-in. E6011 electrodes. Subsequent passes on this beveled side were made with $^3/_{16}$-in. E6020 electrodes to fill the groove. All weld beads on the bevel side were deposited starting at the outer edge and progressing inward to the web.

5. After the bevel on this flange was completely welded, the beam was turned over and the root of this weld was flame gouged to sound metal. It was necessary to gouge deeply in the vicinity of the web-flange intersection.

6. Two passes were deposited on the root side of the flange using $^3/_{16}$-in. E6020 electrodes to complete this flange weld. These welds on the root side were run from edge to edge.

7. One flange was completed before starting on the other flange, which was welded in the same manner.

8. Outer ends of both flange welds were flame gouged to sound metal, rewelded and ground to the original beam contour.

The results of impact tests on these beams are summarized in Table 10.

The fractures developed in these welded beams without web holes are briefly described as follows:

Beam 9A (Tested at −60° F). The fracture was completely outside the weld zone. Starting in the lower flange, $2^1/_2$ in. from the centerline of the splice, the crack extended vertically in the web for about 9 in. and then branched toward the upper flange, which remained intact. The crack configuration was similar to that shown on Plate 14, with an additional crack branching to the right at the top. The texture was ragged to granular. As a possible explanation of the location of the fracture in the base metal remote from the welded splice, it should be noted that a rod for deflection measurement, previously described, was welded to the bottom surface of the flange. The flange fracture occurred through the outer edge of this localized weld zone. Although a deflection rod was similarly welded on the previously tested beams without apparent influence, it is possible that a detrimental condition existed which was made effective by the greater resistance of the continuous splice. However, Beam 9A1, also had a welded rod attached in the same manner and buckled without fracture at −40° F. Because of the possible influence of the welding of the deflection rod, all of the remaining beams were tested without deflection measurements. If the fracture was induced by the localized weld on the centerline of the flange without affecting the geometry, the cause was probably due to a metallurgical discontinuity producing a stress concentration.

Table 10—Impact Tests on Beams with Continuous Weld Splices

Beam No.	*Steel treatment*	*Test temp., ° F*	*Height of fall, in.*	*Permanent deflection set*	*Failure*
9A1		−40	90	1.15	Buckled
9E		−50	90	0.99	Buckled
9A		−60	84	0.54	Fracture, bottom flange and web
9B1	FK, [illegible]	−65	96	0.91	Buckled
9B	as-welded	−80	33	0	Fracture, bottom flange and web
9E1		−90	21	0	Fracture, complete

Beam 9B (Tested at −80° F). The fracture occurred in the weld metal of the flange weld and in the web weld to a height of about 3 in. The cracks then branched symmetrically in the web base metal, terminating at the upper flange fillets, leaving the upper flange intact. The crack pattern was similar to that shown on Plate 6, with the branch occurring nearer the lower flange. The fracture was fine grain around the beam fillet with the remainder ragged granular. The fracture apparently started at a V-shaped void $^5/_8$ in. long tapering from $^3/_4$ to $^1/_8$ in. wide located in the fillet zone. The surface of this void was oxidized by the flame gouging and indicates lack of penetration at the junction of the web and flange welds.

Beam 9E1 (Tested at −90° F). The complete fracture included both flanges and the web. The lower flange and lower 1 in. of the web fractured in the weld metal. The remainder of the web fracture and the upper flange fracture was about 2 in. from the splice.

The crack pattern was somewhat like Plate 9, except that the lower flange fractured through the weld and the web crack left the weld near the lower fillet. The lower flange fracture showed a small pinhole within an irregular texture indicating lack of complete fusion at the web-flange weld junctions.

In making this type of continuous web-flange welded splice, it was expected that some difficulty would be encountered in getting complete fusion in the fillet region. In order to determine the soundness of these welds, the welds were cut in a series of sections at both flange-web junctions of each of the six beams, after test. The findings of these examinations are:

Beam 9A1 (−40° F—buckled). Bottom flange: No visible imperfections. Top flange: Single void 3/16 x 1/16 x 1/16 in. deep.
Beam 9E (−50° F—Buckled). No flaws in either flange weld.
Beam 9A (−60° F—fractured). Bottom flange: Small pinhole. Top flange: Single void 1/4 x 1/16 x 1/16 in. deep, located 3/8 in. from centerline of web and 1/4 in. from outside surface of flange.
Beam 9B1 (−65° F—fractured). Bottom flange: Single void 1/8 x 1/16 x 1/8 in. deep at mid-thickness of flange, 3/8 in. from web centerline. Top flange: No visible flaws.
Beam 9B (−80° F—fractured). Bottom flange: Flame gouged surface exposed in V-shaped void 5/8 in. long tapering from 3/8 to 1/8 in. wide. Top flange: small pinholes in weld junction.
Beam 9E1 (−90° F—fractured). Bottom flange: 1/16-in. diam hole in weld. Top flange: Single void 1/4 in. diam x 1/16 in. deep in weld.

No defects were found in the fillet zone of the web weld.

Transition Temperature

Based on the fracture-buckle criterion, the data indicate a probability of failure by brittle fracture below about −70° F. This value neglects the fracture produced at −60° F because of the uncertainty of the effect of the subsequent localized weld in attaching the deflection rod and also neglects the effect of a sizable flaw in the weld of the beam tested at −80° F.

In any event, a comparison with the results obtained from the FK steel beams with web holes shows a decided improvement of performance. The continuous weld splice appears to have lowered the critical temperature by approximately 40° F.

The imperfections found in these welds are perhaps no greater than those frequently found in welds made under similar conditions, except for the sizable void found in one beam (9B). This unfused void does emphasize the need for special precautions in back gouging and back welding of the flange in making this type of splice which, however, should not introduce unreasonable limitations on the shop procedure.

The improved performance at low temperature of these continuous weld splices notwithstanding imperfections in the welds at the flange-web weld junction confirms the conclusion that the presence of web holes invited the start of cracks and thereby decreased the low-temperature resistance of the splice. The shop report and these test data justify the conclusion that the continuous weld splice is practical and preferred for low-temperature service.

SUMMARY AND CONCLUSION

The original objective of this project was the determination of the relative resistance and transition temperatures of structural steel beams, composed of SK and FK steels, fabricated with butt-welded splices and tested as-welded, after stress-relieving treatment and when welded with preheat. The results of this program of tests, the major part of which involved impact loading, suggested further investigation of welding and fabrication variables and these by-products furnish additional information on associated controversial questions. The analysis and interpretation of accumulated supplementary data contribute observations which are of interest in the design of welded splices and further study of the brittle fracture phenomena.

The findings are briefly summarized as follows:

1. Charpy tests on material taken from the rolled sections show a decrease of "notch sensitivity" by silicon-aluminum deoxidation. The Charpy values show a variation of about 20° F and 40° F for the SK and FK steels, respectively, depending on location in the rolled section. In the case of the FK steel, part of this variation was due to fissures developed transverse to the fracture. The material adjacent to the beam fillets in both the flange and web had the highest transition temperature. Comparisons at similar locations indicate lower transition temperatures for the FK steel by 50 to 75° F.

2. Impact tests on FK steel beams butt welded with E6020 electrodes show lower transition temperatures than the SK steel beams by 24° F in the as-welded condition and by 40° F after "stress relief" and when welded with preheat based on a fracture vs. buckle criterion. Welding with preheat was beneficial in both steels with E6020 electrodes, although to a greater extent in the case of beams made of FK steel. The results suggest the use of preheat on SK steel as an alternate to FK steel in the as-welded condition.

3. Strain measurements during both static and dynamic tests showed large strain concentrations in the vicinity of the open web holes introduced to facilitate the welding of the flanges. In most cases, fractures started at the web hole which acted as an internal notch, although at very low temperatures, fractures occurred in the base metal without regard to the splice.

4. Comparisons of the relative behavior of the welded beams was based on a fracture vs. buckle criterion. The extent of fracture varied with decreasing temperature from a small crack or tear at the web hole to complete shattering fracture of the entire section. At temperatures above those producing cracks, the beams failed by normal flange buckling. Test temperatures ranged from −120 to +60° F. The extent of the cracks produced showed a definite relation to the test temperature. A "shear lip" found at the edges of free-running cracks is associated with prior internal fracture.

5. The proportional limit and structural yield point of the beams under impact loading was determined

from derived equivalent static load-deflection curves. The proportional limit was reduced in these as-welded beams by residual stresses which were effectively minimized by stress-relieving treatment. The combined effect of decreased temperature and increased strain rate raised the elastic range of flange stresses to approximately double the static values at room temperature.

6. A limited number of tests on unwelded beams with drilled web holes in which a pressed notch served as a crack starter, showed a decided difference in the ability of the two materials to resist free-running cracks. The tendency to propagate a crack is more pronounced in the SK steel. To produce the same length of initial crack requires about 60° F lower temperature in the case of the FK steel. It appears that for the specimen and test procedure used in this investigation, cracks do not propagate above some critical temperature. The lowest temperature inhibiting free-running cracks is a more rational and useful definition of transition temperature than one based on factors related to crack initiation.

7. Static tests were inconclusive in determining resistance to brittle fracture at temperatures as low as −120° F. Under impact tests, brittle fractures were produced at temperatures up to 0° F. The strain rates produced in these impact tests were of the order of 0.8 to 1.0 in./in./sec. The time to maximum strain and deflection was about 0.007 sec, varying slightly with temperature.

8. Scratch measurements of deformation made after failure showed a marked decrease of plastic deformation preceding fracture or buckle with decrease of temperature, from 0.02% (limit of measurement) at −115° F to 8% in the flange and 26% in the web at the hole, at +30° F.

9. The presence of web holes introduced a critical condition for the initiation of cracks primarily because of the notch geometry involved. The finishing of these holes by flame cutting may have had some influence on the fractures particularly at the corners adjacent to the flange. Tests on unwelded beams with flame-pierced and finished holes showed transition temperatures approaching those of the welded beams. Beams with drilled and filled holes did not fracture at −115° F, the lowest test temperature. Examination of flame cut edges of the hole showed thermal cracks extending into the fusion zone about 0.002 in.

10. Tests of beams welded with low-hydrogen electrodes for comparison with similar beams welded with E6020 electrodes used in the major program, show:

(*a*) The use of low-hydrogen electrodes necessitates adequate moisture control and correct manipulation, if the best results are to be obtained.

(*b*) The E6016 electrodes used without moisture control were taken from normal storage. These electrodes, as used, on SK steel lowered the transition temperature by 20° F in both the as-welded and preheat conditions. The uncontrolled E6016 electrodes as here used on SK steel in the as-welded condition produced about the same favorable effect as welding with preheat and E6020 electrodes. Preheat was beneficial. It cannot, however, be concluded that the effect of preheat is superimposed on that normally expected with low-hydrogen electrodes because of the uncontrolled moisture content.

(*c*) The use of E6016 electrodes without moisture control was unfavorable when applied to the FK steel in both the as-welded and preheat conditions. The transition temperatures were raised by 15 and 20° F, respectively. The exactly opposite effect on the two grades of steel indicate that a generalization as to the effectiveness of E6016 electrodes as used in these tests is not warranted. It should be noted that the E6016 electrodes used in these weldments were taken from normal storage without moisture control and the moisture content may have been variable. The assumed unfavorable influence of moisture does not explain the inconsistency in behavior of the two steels, unless the absorbed moisture was unusually high in the case of the electrodes used for the FK steel beams first welded.

(*d*) A comparison of weldments made with E6016 electrodes with moisture control is limited to beams composed of FK steel. These moisture-controlled low-hydrogen electrodes lowered the transition temperature by 35° F as compared with the E6020 electrodes in the as-welded condition and apparently overcame the unfavorable effects of E6016 electrodes without moisture control. When welded with preheat the moisture controlled electrodes gave no improvement over the E6020 electrodes with the same preheat treatment. The results indicate that weldments made with these electrodes in the as-welded condition were equal to those fabricated with E6020 electrodes and preheat treatment for the FK steel used in this investigation. The transition temperature was the lowest (−65° F) of any found in the tests with the web-hole type of splice.

11. Alternate fabrication procedures aiming to avoid the unfavorable effects of the web holes adjacent to the flanges have been suggested. Butt weldments made by continuous welding of flanges and web without holes require careful attention to the condition of the weld in the fillet region but this type of splice is entirely practical. A series of impact tests on beams with this type of splice, using E6020 electrodes for comparison with similar beams fabricated with web holes, show that the continuous weld splice of FK steel beams in the as-welded condition had a transition temperature of about −70° F which is 40° F lower than similar beams with web holes. Examination of the welds indicated some flaws at the flange-web weld junction, a condition which can be overcome by precautions in the welding operation. The test results indicate a decided improvement in low-temperature performance with this type of splice.

12. The investigation reported here started with limited objectives but, with the accumulation of data

and observations, the studies were extended to provide information dealing with fabrication and splice design. These by-products are a natural outgrowth of continuing study and themselves suggest the need for further investigation of the adequacy of welded-beam fabrication and beam splices. Other possible studies of interest to the designer include (1) optimum preheat temperatures particularly for thick materials; (2) influence of the properties and grade of steel on the effectiveness of different types of electrodes; (3) further study of the effect of hydrogen and moisture control of electrodes; (4) alternate designs of butt splices in beams; (5) resistance to fracture at low temperatures of built-up beams with heavy flanges and web-flange fillet welds.

ACKNOWLEDGMENTS

The authors wish to acknowledge the joint sponsorship of this investigation by the office of Naval Research and the Welding Research Council, which made this project possible. The advice and recommendations of the Advisory Committee is greatly appreciated. The interest and cooperation of LaMotte Grover, Chairman of the Structural Committee and William Spraragen, Director of the Welding Research Council, have been particularly helpful. We wish to thank the Bethlehem Steel Co. for its cooperation in the many details of materials and fabrication of beam specimens. Through the generous contribution of the Research Corporation to this investigation, the instrumentation used was made possible. Acknowledgment is due the members of our laboratory staff and especially Prof. E. C. Ingalls, formerly a member of the staff, for his contributions in the development of instrumentation and conduct of many of the tests.

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Appendix

Metallurgical Report submitted by Bethlehem Steel Co. for this project including melting and Mill Practice and Fabrication of Beams

PART I. CHEMICAL ANALYSIS (see Table 1 of Report)

PART II. MELTING PRACTICE

Practice	*34J333*	*35J272*
Process	Basic open hearth	Basic open hearth
Materials charged:		
Limestone, lb	18,000	20,000
Iron ore, %	None	4.8
Cold scrap, %	53.4	34.5
Hot metal, %	46.6	60.6
Metallic charge, lb	258,300	286,960
Carbon at melt	0.58	1.50
Time: Charge to melt	9:20	7:10
Working materials:		
Iron ore, lb	7000	12,000
Time: Last ore addition to tap	1:03	2:20
80% FeMn, lb	None	1500
Roll scale, lb	1000	None
Limestone, lb	2000	None
Fluorspar, lb	500	500
Burnt lime, lb	None	1500
FeO: Prior to additions, %	13.6	12.5
Furnace additions:		
80% FeMn, lb	500	3500
Time: Melt to tap	1:13	3:35
Total time	10:33	10:45
Ladle additions:		
80% FeMn, lb	1000	None
50% FeSi, lb	200	1400
Aluminum, lb	None	270
Coal, lb	480	None
Ingot data:		
Mold additions:		
Aluminum, oz per mold	15	None
Size, in.	25 x 30 x 88	23 x 29 x 89
Design	Open top, big end down	Sinkhead, big end up
Condition of tops	Flat	
Sinkhead insulation	None	Slag wool
Weight, lb	15,860	13,300
Number made	15	19
Number applied	5	10
Time intervals:		
(*a*) Tap to teem, min	13	15
(*b*) Teem to charge in soaking pits, min	40	105
(*c*) In soaking pits, hr	7	5.5
Temperatures:		
(*a*) Ingots at draw, ° F	2230/2235	2200/2280
(*b*) Beams at finishing mill, ° F	1760/1780	1700/1800

PART III. MILL PRACTICE

Rolling Practice. All ingots were rolled bottom first. The ingots in each heat were numbered in the order of rolling. This also indicates the relative order in which they were poured, but not their position in the heats. On heat 34J333, ingots 1 through 5 correspond to ingots 5 through 9 of the 15 ingots poured. On Heat 35J272, ingots 1 through 10 correspond to ingots 5 through 14 of the 19 ingots poured.

Cutting Practice. Five 28-ft beams were cut from each ingot. Only the bottom five multiples of each ingot were applied to this order.

A 4-ft test piece was cut from the top portion of the bottom (*E*) cut of the third ingot rolled on each heat (ingot No. 7 on both heats).

Hot Bed Practice. The beams were placed on one hot bed (approximate capacity 100 beams) with the flanges toe to toe (web vertical). This is the conventional procedure for cooling beams of this section. Only one hot bed was utilized in cooling to enhance accuracy in identification.

Identification. Each beam was stamped on the flange directly above the web with the heat number, ingot number (order of roll) and cut number (*E* through *A*, bottom to top).

Straightening. The beams were gag straightened when cold to commercial tolerance.

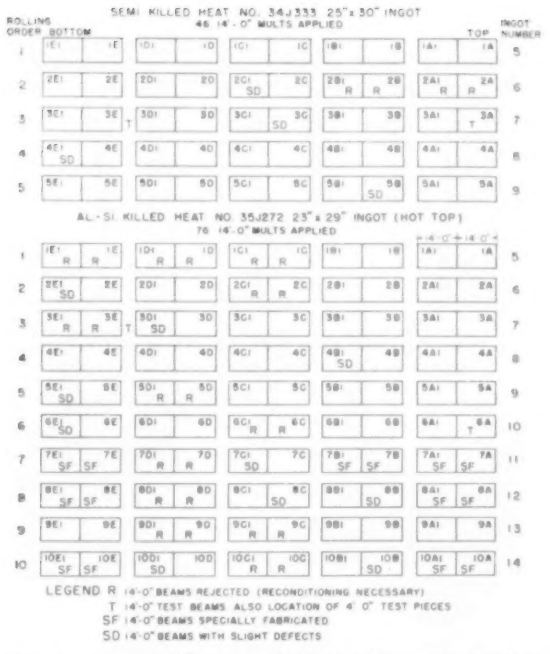
SEMI KILLED HEAT NO. 34J333 25" x 30" INGOT
46 14'-0" MULTS APPLIED
ROLLING ORDER BOTTOM
TOP
INGOT NUMBER
AL-SI KILLED HEAT NO. 35J272 23" x 29" INGOT (HOT TOP)
76 14'-0" MULTS APPLIED
LEGEND R 14'-0" BEAMS REJECTED (RECONDITIONING NECESSARY)
T 14'-0" TEST BEAMS, ALSO LOCATION OF 4'-0" TEST PIECES
SF 14'-0" BEAMS SPECIALLY FABRICATED
SD 14'-0" BEAMS WITH SLIGHT DEFECTS

Fig. 26 Status of Beams—Columbia University Project

Inspection. All beams were carefully inspected for surface defects, laminations, straightness and length.

Piling. The *A*, *B*, *C*, *D*, and *E* cuts of each heat were piled in separate lifts and stored under cover for future use as requested by the Structural Steel Committee.

Figure 26 shows the status of each beam rolled for the Columbia University Project.

PART IV. FABRICATION OF BEAMS

Selection of Beams. The beams from Heat 35J272 were designated for special fabrication by the Structural Steel Committee of the Welding Research Council.* Following is the welding procedure used in the fabrication.

Filler Metal. Root Passes: $^5/_{32}$-in. diam AWS-ASTM E6011 welding rod. Subsequent Passes: $^3/_{16}$-in. diam AWS-ASTM E6020 welding rod.

Process. Arc welded.

Equipment. The supply of welding current was provided by a 500-amp, 25-cycle a-c transformer.

Position of Welding. Beams were mounted in a specially constructed positioning device. This permitted the beams to be rotated about their longitudinal axis so that welding could be done in the flat position.

Preparation of Base Metal. The web and flange surfaces to be joined by welding were beveled by machine flame cutting to the lines and dimensions shown on the Bethlehem Steel Co. Drawing No. DL-197-$^1/_4$ (see Fig. 7).

Tacking. The flanges were tack welded (about 1 in.) at four points as shown in Detail A, Fig. 27. The tacks were placed on the outer flange surface and were removed by gouging prior to placing Pass 3 as explained in the paragraph Flange-Joint Welding below.

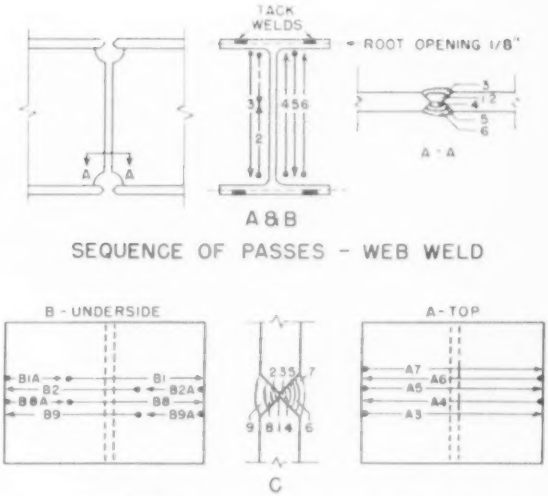

Figure 27

Web-Joint Welding. The web-joint was welded first, with one operator performing all the welding. The sequence and direction of welding is shown in Detail B in Fig. 27. Root passes 1 and 2 were made using a $^5/_{32}$-in. diam E6011 welding rod. Pass 3 which was made with a $^3/_{16}$-in. diam E6020 welding rod completed one side of the web joint. The beam was then rotated 180 deg. After back gouging to sound metal, Passes 4, 5 and 6 were made with the $^3/_{16}$-in. diam E6020 welding rod to complete the weldment.

The coped holes at each end of the web were hand burned back to assure sound metal at the end of the weld.

Flange-Joint Welding. The sequence and direction of welding is shown in Detail C in Fig. 27. Root Pass B1-B1A* was placed on the underside of the flange using the $^5/_{32}$-in. diam E6011 welding rod. Pass B2-B2A was then placed using the $^3/_{16}$-in. diam E6020 welding rod. The beam was rotated 180 deg and back gouged to sound metal. The outside of the flange was then completed by placing Passes A3, A4, A5, A6 and A7 with the $^3/_{16}$-in. diam E6020 welding rod. The beam was again rotated 180 deg and Passes B8-B8A and B9-B9A were made on the underside of the flange with the $^3/_{16}$-in. diam E6020 welding rod to complete the weldment. The same procedure was used in welding the opposite flange joint.

The overflow metal at each of the four flange-joint edges was back gouged to sound metal, welded and hand ground level with the flange edge.

Special Heating. Beams 7A and 8E were preheated

* Refers to first lot fabricated. Following procedure applies to all beams furnished.

* Passes on the underside of the flange were made in two parts (Detail C, Fig. 27). For example: The first part of Pass 1 was made from the starting point toward the flange edge as indicated by the arrow *B1*. The second part of Pass 1 was made from the opposite flange edge to the starting point of *B1* as indicated by the arrow *B1A*.

and welded at an interpass temperature of 350° F (measured by Tempilstik). Beams 7A1 and 7E1 were stress relieved subsequent to welding and prior to shipment. The stress relieving consisted of heating to 1150° F, hold 2 hr, furnace cool to 500° F and air cool.

PART V

Results of physical tests made at the mill are included with data shown in Tables 2, 3 and 4.

Stress Corrosion Cracking of Welded Low-Alloy Steels

by E. Baerlecken and W. Hirsch

SUPPLEMENTING recent German work by Rädeker on aluminum steels, the present authors show that greater resistance to stress corrosion cracking is afforded by low-alloy steels containing titanium or tantalum plus columbium. Rings cut from arc-welded pipe are forced on tapered bars to expand their diameter 5, 10, 20 and 30%. Boiling calcium ammonium nitrate solution is the test solution. The results are summarized in Table 1.

The aluminum steel was less resistant to cracking than the Ti and Ta-Cb steels. The as-welded specimens of Steel Al failed in the heat-affected zone. The failure of the heat-treated specimens welded with A and B occurred in a narrow decarburized zone at the fusion line. The Ti and Ta-Cb steels were completely resistant when welded with 18-8 electrodes. With weld metal B, which had high Ta+Cb content to offset loss in welding, the Ti and Ta-Cb steels were resistant after heat treatment. The Cr-Mo weld metal C did not contain high enough Ti or Ta+Cb to be resistant. With gas welding the Ta-Cb steel was superior to the Ti steel.

Table 1—Results of Stress Corrosion Tests on Three Low-Alloys Steels

		Percentage of arc-welded specimens that failed after 21 days		
Steel	*Treatment*	*Weld metal A*	*Weld metal B*	*Weld metal C*
Al	As-welded	50	30	100
Al	Annealed*	100	100	50
Ti	As-welded	0	100	100
Ti	Annealed*	0	0	30
Ta + Cb	As-welded	0	30	100
Ta + Cb	Annealed*	0	0	30

Composition of steels and weld metals

Material	*C*	*Mn*	*Si*	*Cr*
Steel Al	0.27	0.56	0.23	
Steel Ti	0.07	0.50	0.50	
Steel Ta + Cb	0.08	0.29	0.26	
Weld metal A	0.06	0.54	1.32	18.7
Weld metal B	0.065	0.35	0.22	
Weld metal C	0.12	0.54	0.31	1.32

Material	*Ni*	*Mo*	*Al*	*Ti*	*Ta + Cb*
Steel Al			0.08		
Steel Ti				0.58	
Steel Ta + Cb					0.92
Weld metal A	8.9				0.86
Weld metal B					2.27
Weld metal C		0.46			

* Heated 30 min at 750° C and cooled in air.

Abstract of "Interkristalline Spannungsrisskorrosion unlegierter und niedriglegierter Stähle und ihre Vermeidung durch Titan-oder Tantal-Niob-Zusatz," published in *Stahl u. Eisen*, **73**, 785-789 (June 4, 1953). Abstracted by Dr. G. E. Claussen.

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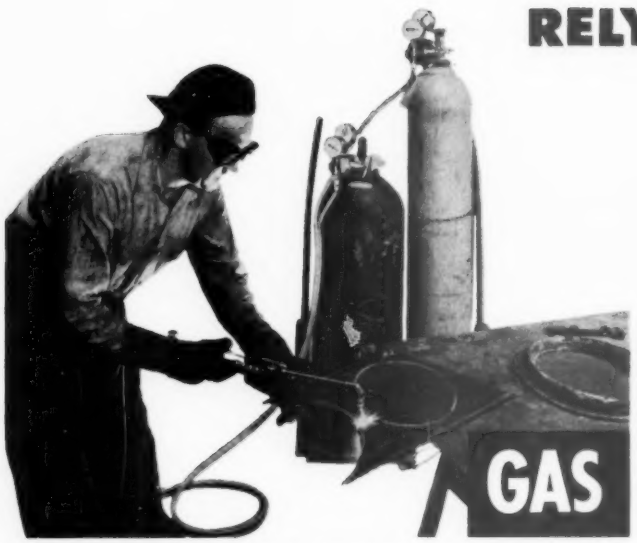

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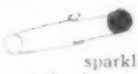

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